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The characterisation and numerical modelling of viscoelastic polyurethane foams for use in custom wheelchair seating

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Doctor of Philosophy (PhD)

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November, 2012

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Abstract

Viscoelastic polyurethane foam is widely used in wheelchair cushions as it offers good pressure relieving capabilities. However, the behaviour of this material is largely un-quantified, by comparison with conventional elastomeric materials. Consequently, in many cases, inadequate cushioning is provided to wheelchair users with complex seating requirements. This thesis characterises and numerically models viscoelastic polyurethane foam.

Temperature-dependent static compression and simple shear test procedures are conducted on a range of viscoelastic polyurethane foams and selected results are utilised to identify Ogden Hyperfoam material model parameters. Time-dependent creep and stress relaxation test procedures are conducted and test results are used in conjunction with Time-Temperature-Superposition (TTS), William-Landel-Ferry (WLF) and Arrhenius theories to generate long-term predictions of material behaviour. Appropriate spring-dashpot models are utilised to model predicted long-term viscoelastic performance. Thermal conductivity parameters are obtained using Glicksman’s theoretical model. The accuracy of predictions obtained using TTS and WLF theories has been proven. Validation has also been achieved for the temperature-dependent Hyperfoam, long-term viscoelastic and thermal conductivity parameters.

The range of fully validated material model parameters were utilised to simulate the in-service seating behaviour of polyurethane foam. Simulation results were relatively compared and analysed with respect to relevant pressure ulcer risk factors. From analyses of the FE simulations, results lend support to findings from clinical trials particularly with respect to the relationship between shear and direct pressure in wheelchair seating. Other FE results disagree with accepted seat prescription timings used in current clinical practises.
Declaration

I certify that this thesis which I now submit for examination for the award of Doctor of Philosophy (PhD), is entirely my own work and has not been taken from the work of others, save and to the extent that such work has been cited and acknowledged within the text of my work.

This thesis was prepared according to the regulations for postgraduate study by research of the Dublin Institute of Technology and has not been submitted in whole or in part for another award in any other third level institution.

The work reported on in this thesis conforms to the principles and requirements of the DIT's guidelines for ethics in research.

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Signature ______________________________ Date _______________

Candidate
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Chapter 1 Introduction

Research defining the physical characteristics of flexible polymeric foam is described in this thesis. In this chapter, the rationale for the research is introduced and its relevance to applications for these materials is outlined. The central research question is posed and the project aim that stems from this question and the resulting primary objectives are specified. The methodology used in the completion of the proposed objectives is then introduced. Following this, the contribution of the thesis to the existing knowledge is outlined. Finally, the thesis structure, describing briefly the contents of each chapter, is described.

1.1 Background

Foam is an important engineering material having multiple applications over a range of industries, finding its way into numerous products such as furniture, bedding, packaging, sports and medical equipment, automotive and aerospace components. Flexible Polyurethane Foam (FPF) is a type of hyperelastic open celled polymer foam which can be subjected to large reversible deformations. This behaviour, combined with the properties of the constituent polymer (polyurethane rubber) make polyurethane foam suitable for use in those industries where seating plays an important role, as it can provide pressure relief in the seat-body interface region.

The provision of seating for wheelchairs is a specialist area with many challenges not encountered in the design of conventional automobile or domestic furniture seating. For a number of reasons, including physical and neurological disabilities, some wheelchair users may not be able to shift their weight during sitting. Over
extended time periods, areas of high pressure may develop at the seat-body interface. Pressure ulcers frequently develop as a result of the compressive and shear interfacial forces in this region. It has also been shown that high pressure gradients, which can occur as a result of higher pressure radiating from the sharp prominent bones in the pelvic region during seating, such as the Ischial Tuberosities or the Coccyx, can also lead to the development of pressure ulcers [1].

![Diagram of the pelvis, with bony prominences highlighted.](image)

Figure 1.1: Diagram of the pelvis, with bony prominences highlighted.

1.2 Research Justification

Polyurethane foam is used in all areas of seating including applications which may require specialised pressure relief performance. Wheelchair users are generally at high risk of pressure ulcer development in the trunk region. Consequently, prescribing clinicians are often required to provide optimal pressure relieving personalised cushions which must be defined within time constraints imposed by the user’s allotted appointment slot. A lack of quantitative knowledge of the in-service performance of time and temperature dependent polyurethane foam means that it is often difficult to create an optimal cushioning system; current procedures are heavily reliant on clinical legacy experience and
empiricism. As a result, subsequent re-fittings are often required. The lack of quantitative knowledge of long term material in-service performance and the resulting difficulties in the chosen application of seat cushioning design was the main justification for this research. To provide an accurate description of material behaviour, it also became necessary to apply advanced and novel techniques to the process of characterising the long-term properties of the materials in question. This work provides a comprehensive behavioural model for these materials, which can be applied to improve understanding of seating functionality. The clinician will benefit through improved understanding of in-service material performance, while the user may benefit through optimal pressure relief performance which ultimately may result in lower rates of pressure ulcer development.

1.3 The Research Question

The central question posed in this research was:

Can temperature dependent hyperelastic, long-term viscoelastic and thermal conductivity material parameters for viscoelastic FPF be identified and validated from relatively short-term test procedures?

1.4 Research Aim

A primary aim of this research was to characterise the behaviour of FPF material in conditions that replicate its normal use over protracted periods. This aim was achieved through the identification of accurate material parameters for a temperature dependent compressible hyperelastic material model and for a long-term viscoelastic mathematical model. Thermal heat transfer material parameters
were also identified for polymeric material. This combination of models allowed for a time and temperature dependent description of material deformation behaviour. Validation was carried out using two physical indentation procedures, a long-term creep test, a high temperature creep test and a material heating test. The results of simulations involving the material parameters described here helped to characterise material behaviour by analysing relative material performance in an idealised seating situation.

1.5 Research Objectives

Specific sub-objectives to assist in the achievement of the primary aim were to:

- Carry out static axial compression and simple shear material tests on a range of commonly used polyurethane foams over a range of strain rates and temperatures.
- Use the test results to identify sets of parameters for the chosen hyperelastic material model.
- Conduct time dependent stress relaxation and creep tests on the same materials.
- Use results in conjunction with Time Temperature Superposition (TTS) and the Williams, Landel and Ferry (WLF) theory to create predictions for material behaviour over long time-scales and at high temperatures.
- Use the results of these original tests and TTS-WLF predictions to identify suitable parameters for the chosen viscoelastic material model.
• Conduct two indentation test procedures on the same materials, a relatively simple flat circular indenter shape and a more realistic seated buttock shaped indenter shape.

• Recreate these indentation procedures in simulations created with the FE package Abaqus [2].

• Compare physical indentation results with FE simulation results, culminating in the validation of hyperelastic and viscoelastic material parameters.

• Conduct a sample heating procedure in a controlled and measureable environment.

• Generate thermal conductivity parameters using a theoretical model. Recreate the sample heating procedure with Abaqus software using the thermal parameters from the model and compare results with the physical heating procedure.

• Construct FE simulation case studies to compare a range of cushion designs.

• Assess the performance of cushion designs to potentially provide guidance in the provision of an optimal cushion.

1.6 Research Methodology

The research methodology consisted of the following elements:

1. A review of existing literature including:
   - Material overview.
   - Relevant material models and their applications.
o Clinical literature pertaining to the development of pressure ulcers.

2. Test rig design and construction including:
   o Uniaxial compression and simple shear test rigs for use in a Lloyd LR30K materials testing machine.
   o Raised temperature uniaxial testing rig.
   o Flat bottom, circular indenter testing rig.
   o Raised temperature creep compression test rig.

3. Physical testing on viscoelastic polyurethane foam materials including:
   o Uniaxial and stress relaxation testing over a range of strain rates and temperatures.
   o Simple shear testing.
   o Compressive creep testing over a range of temperatures.
   o Flat bottom, circular indentation testing.
   o Curved bottom, buttock shaped indentation testing.
   o Sample heating testing.

4. Generation of long term creep compression predictions and high temperature creep compression predictions using data collected from creep compression testing procedures.

5. Identification of accurate material model parameters using data collected from testing procedures and using predictions generated from long-term predictions.

6. Validation of hyperelastic, viscoelastic and thermal conductivity material model parameters using results from the two indentation test procedures and a sample heating test.
7. Creation of FE model case studies whose results allow for time and temperature dependent analysis of different seating designs. Simulations using accurate material models can provide quantitatively based results, which can in turn be used to inform the prescription of cushions.

1.7 Contributions to Knowledge

The recent history of using FE in clinical seating has focused on modelling the aetiology of pressure ulcers. Many authors have simulated the seated human and analysed the stress–strain fields in the trunk region [1, 3-8]. It has been found that the highest pressures predominantly radiate outwards from the Ischial Tuberosities. However, within these simulations an accurate material model of the cushioning material has never been used. In this work, unlike most previous research in the area, the concentration is solely on the material involved in cushioning, with the aim being to optimise the cushioning material involved in wheelchair seating.

Strain energy based material models have been used extensively throughout the literature in modelling many different types of systems and materials, including seating foam. Identifying appropriate temperature and time dependent hyperelastic and long-term viscoelastic material parameters is an improvement on current polyurethane seating foam modelling methodologies.

Hyperelastic model parameters have been validated using realistic ISO buttock-shaped indentation testing procedures. Validating thermal conductivity material parameters using an inverse modelling technique is another method of parameter validation.
As far as the author is aware, no research into the design of seating has been conducted previously using a thermally enabled structural hyperelastic and viscoelastic material model with heat transfer properties. A section of the literature review in this thesis describes the effects of temperature in seating, highlighting the requirement for such a model.

The generation of both long term and high temperature creep compressive behaviour predictions for FPF is also a novel contribution. Predictions generated using TTS-WLF theory and physical test data have also been compared with predictions generated from the Arrhenius model which is based on the chemical reaction rate of degradation of the material during testing.

The thermal-structural model was utilised in commercial FE software and results from the simulated cushion-indenter interface such as contact pressure, time and contact shear force were analysed. The results allow for:

- An analysis of the important pressure ulcer risk factors (pressure and shear), in an idealised seating simulation, which may help to improve understanding of the origin of discomfort in seating.
- A comparison of interface pressure over time, which allows improved understanding of material time dependency in-service.
- A more detailed understanding of the relationship between pressure and shear using a range of different seating cushion designs.

The FE results help improve current understanding of in-service material behaviour. This could eventually lead to more accurate cushion design and cushion fit, reduced fitting costs, reduced fitting time and reduced the need for refitting. Ultimately, this work has instigated developments which this may lead
to a reduction of high pressure and high shear force values in seating, which will result in a reduction of pressure ulcer occurrence.

1.8 Thesis Structure

This thesis has three separate background research regions: materials, modelling and clinical application. The polyurethane foam material being used throughout the study is introduced in Chapter 2. The chemical and physical constructions of polyurethane foam are discussed as are its physical deformation characteristics. In Chapter 3, the structural, thermal and long-term prediction models used throughout the thesis are introduced and derived. The clinical aspects of this research are discussed in Chapter 4. All of the testing procedures carried out in this research are introduced in Chapter 5. The procedures are broken into static, time dependent and validation test procedures. In Chapter 6, the results of the static and time dependent test procedures are presented. Following this, material model coefficients are identified in Chapter 7. Appropriate material data sets, curve-fitting and material inspection exercises are used. In Chapter 8, these material coefficients are used in appropriate simulations and validation of the different aspects of the model ensues. Chapter 9 introduces the simulations which utilise the material models validated previously. The results from these simulations are analysed and provide some useful outcomes. Chapter 10 presents conclusions for the work that reflect the research aims and objectives. Recommendations for future work are also provided in this chapter. Appropriate appendices to this work are supplied at the end of the thesis.
Chapter 2 Polyurethane Foam

This chapter combined with the 3rd and 4th chapters of this thesis represent a comprehensive literature review of all relevant areas pertaining to this work. This research centred on a specific set of polyurethane foams. Polyurethane rubber, which is the material from which polyurethane foam is manufactured, is introduced in this chapter. The chemical reactions and the procedures involved in the manufacture of polyurethane foam are also presented. The microstructural behaviour of the material is then described. Finally, the macrostructural behaviour is investigated, including descriptions of hyperelastic and viscoelastic behaviour.

2.1 Constituent elastomer

In an elastomeric foam material, the individual cells are constructed from a material known as the constituent elastomer. The constituent elastomer of polyurethane foam is polyurethane rubber. This elastomeric material can undergo large and reversible deformations. The wide range of processing methods and resulting mechanical properties combine to make polyurethane a versatile polymeric material. With careful selection of reactants and manufacturing processes, the resulting polyurethane can be, for example, a rigid crystalline plastic, FPF, or viscoelastic gel. Conventional elastomers, such as polyurethane, are composed mainly of long chain molecules in a macromolecular network. The fundamental molecular elastomeric network generally consists of long chain molecules with chemical and physical cross-links providing a three dimensional, generally amorphous solid structure, as depicted in two dimensions in Figure 2.1.
2.2 Foam Production

FPF is produced from the chemical synthesis of raw material reactants. The use of different material reactants can lead to variations in constituent elastomer properties. The chemical reaction between reactants also influences the microstructure of the final material.

2.2.1 Reactants

The two raw material reactants used in the production of FPF are polyols and diisocyanates. The chemical character, structure and molecular size of these reactants have a deciding influence on the course of the reaction as well as the properties and performance of the finished foam product [9, 10]. Additives can also be incorporated to introduce additional properties such as flame retardation.

Polyethers and polyesters have always been the most widely used polyols in the production of foam. Polyesters were almost entirely used from the onset of mass production but this changed around 1958 when the lower cost polyethers gained a foothold in the market. By 1979 polyethers had a ninety percent share of the FPF manufacturing market [11].

Figure 2.1: Diagrammatic representation of polymer chains cross-linked into a network.
Diisocyanates are compounds with two isocyanate groups in the molecule. There are fewer options when choosing a diisocyanate in comparison to the range of polyols available. The two main diisocyanates are TDI (toluene diisocyanates) and MDI (diphenylmethane diisocyanate). TDI is generally the diisocyanate of choice in FPF manufacturing. MDI is mainly used in the production of high resilience, semi-flexible, and microcellular foams [12].

2.2.2 Reactions

There are two main reactions in the production of polyurethane foam: the gelation reaction and the blowing reaction. The gelation (or cross-linking) reaction is the term commonly used for the hydroxyl-isocyanate reaction. If the gelation reaction happens too quickly, tight closed cell foam will be produced. The blowing reaction occurs between water and isocyanate. This reaction produces carbon dioxide which expands the polymer into its final form. If the blow (gas-producing) reaction occurs too quickly, the cells may open before the polymer has enough strength to uphold the cellular structure, resulting in collapse of the foam. For the manufacturer, balancing the respective rates of these two reactions provides the open-celled morphology in the foam which decides its physical properties [12]. Catalysts are commonly used to assist in providing this balance [13].

2.3 FPF manufacturing

Among all polyurethane products, FPF is the largest product family by quantity, accounting for over 40% of all polyurethane usage [14, 15]. FPF has many wide-ranging applications, as shown in Figure 2.2. This work concentrated solely on FPF.
FPF are manufactured via two processes: slabstock (Figure 2.3) and moulded (Figure 2.4) foaming. Slabstock foams are largely used in the furnishing industry. For this process, the reactive components are blended in a transverse mixing head and laid out on a conveyor belt. As the conveyor belt moves forward, the foaming mixture expands and rises to shape. The slabstock processes are essentially continuous operations, in which production can be optimized by changing the volume of the foam slab produced. Cutting large slabstock buns into complex shapes takes time and produces a lot of waste. For this reason moulded foams are largely used in applications where more complex shapes are required, such as automotive seating. This process begins by mixing the reactants together and transferring the foaming mixture to a mould. As the mixture reacts, the foam expands and eventually replicates the shape of the mould. The main drawback in using this batch process is the time required for the process to be completed.
Figure 2.3: Slabstock foam manufacturing procedure, reproduced from Herrington and Hock [10].

Figure 2.4: Moulded foam manufacturing process, reproduced from Herrington and Hock [10].
2.4 Foam microstructure

Micromechanically, FPF is a cellular polymer made up of cell edges, cell voids and cell faces (Figure 2.5). The cell voids are separated by the solid cell edges, which are connected by vertices. Edges are relatively stout, in that their lengths are only a small multiple of their width. In general, the width of the edges reaches a minimum halfway along the length of the edge. The edges are approximately linear and have the characteristic three-cusp hypocycloid cross-section. Each vertex is made up of a junction of generally four ligaments [16, 17]. An open cell material is one which largely consists of open windows leaving many cells interconnected in such a manner that gas may pass freely from one cell to another. This study will concentrate on open celled FPF. Alternatively, closed celled materials are made up of discrete cells through which gases do not pass freely.

Note that for the open cell structure, the bulk of the polymer is in the edges [18]. Foam is a complex and inhomogeneous material; cells of different sizes are randomly distributed throughout the structure.

The ability to undergo repeated large deformations without permanent change in shape is the outstanding characteristic of FPF. As previously alluded to in section 1.1, this characteristic happens for two reasons. It is partly caused by the numerous voids in an open celled lattice structure which allow the material to crush under compression as air escapes and the struts and vertices come together. Secondly, this crushing effect would not be possible without the particular macromolecular structure of the foam’s constituent elastomeric material, polyurethane rubber.
2.4.1 Foam micro structural deformation behaviour

An idealised singular open cell under compressive load goes through three stages of deformation. Initially cell edges begin to bend, this is followed by cell edge buckling and finally cell edges begin to come into contact with each other as the cell collapses. This phenomenon is usually modelled micro structurally by representing the foam as an assemblage of linear elastic beams, whose buckling determines the strain localisation. The cubic-cell unit model of open cell foams has been developed, with different assumptions made by different authors [19, 20]. The tetrakaidecahedron shaped cell proposed by Lord Kelvin [21] was later used to model micro structural behaviour [22]. Several other authors have tried to model the micro structural behaviour of cellular materials [16, 22, 23]. In general however, due to the complexity and inhomogeneous nature of a polyurethane foam structure and due to the difficulty in modelling edges and vertices which come into contact, this approach is computationally demanding [24]. Another approach in studying the structural behaviour of foam is to consider the material as a hyperelastic continuum. This approach was utilised in this study.
2.5 Hyperelasticity

Elastomeric foams exhibit several regions of different stress-strain behaviour in uniaxial compression, (see Figure 2.6(a)): (i) approximately linear behaviour for strains less than about 0.05 – this linear elasticity arises from the bending of the cell edges, (ii) a plateau region in which strain increases at constant or nearly constant stress until a strain of roughly 0.6 – this plateau arises from elastic buckling of the cell edges and (iii) a densification of the collapsed cell edges causing the foam to act similar to its elastomeric constituent material. In this final region, the slope of the stress-strain curve increases exponentially with strain as the crushed foam’s cell struts and vertices come into contact [25]. In tension, (see Figure 2.6(b)), the foam’s behaviour can be separated into two stages: (i) a linear elastic region which is very similar to the first stage of uniaxial compression and (ii) a much stiffer region in which the foam edges rotate out of their settled entanglements and align. The predominant modes of deformation of foam in the majority of functional situations are axial compression and shear.

![Stress-strain curve](image)

Figure 2.6(a): Typical stress-strain compressive behaviour of FPF.
The large strain response of FPF cannot be accurately described using a linear-elastic model. FPF, due to its non-linear elastic response, is classified as hyperelastic. The mechanical behaviour of these hyperelastic polymers is dependent on a number of variables including the material properties of the constituent polymer and temperature effects. Batch effects, moisture and material wear are other variables which are more difficult to account for. Phenomenological theories are commonly used to model the mechanical behaviour of deformed hyperelastic polyurethane foam. Use of phenomenological theory is often quite a challenging procedure because of the hyperelastic behaviour and large deformation mechanisms which are commonly encountered. Further review of the literature concerned with continuum modelling of the structural deformation behaviour of polyurethane foams is described in Chapter 3.

2.6 Viscoelasticity

A viscoelastic material is one which exhibits behaviour which is characteristic of both solids and liquids [26]. An elastic solid has a definite shape and is deformed by external forces into a new equilibrium shape; on removal of these forces the solid will return to its original shape. This can happen because the solid stores all
the energy obtained during the deformation. Viscous liquid by contrast has no
definite shape and flows freely under external force. Polymeric materials are
known to exhibit viscoelastic behaviour, which is dependent on the time scale of
the loading and/or the temperature of the material [27]. Some of the physical
manifestations of viscoelastic behaviour include creep, stress relaxation,
hysteresis and strain rate dependence. Creep and stress relaxation can be inter-
related using the Boltzmann superposition principle.

2.6.1 Creep
Creep is defined as the gradual deformation of a material under a constant load
[28]. The amount of creep is strongly dependent on the material being loaded, the
amount of load, the time over which the load is applied and the temperature of the
material being loaded. A change in any of these variables will cause a change in
creep. The creep response of many polymers and plastics can be loosely defined
by primary, secondary and tertiary regions (see Figure 2.7). The primary region is
the early stage of loading when the creep rate decreases rapidly. The secondary
stage occurs when the creep versus time curve remains constant. Finally the
tertiary stage of creep occurs when the rate of creep increases rapidly.

Understanding long-term creep test data can have important consequences for the
design and manufacture of components made from cellular foam materials [29].
One application, which is of particular interest, is the design of cushions.
Polyurethane foam creep can often directly affect the pressure relief performance
of cushioning systems over long time-scales [30, 31]. The rate of creep in
polyurethane foam has been found to relate directly to the molecular weight of the
polyols used in the material formulation. Increased molecular weight tends to
raise creep rates [32].
Figure 2.7: Loading (left) and reaction (right) of polymeric material held under constant loading, with regions of creep identified.

2.6.2 Stress relaxation

Stress relaxation is defined as a gradual decrease in stress with time under a constant deformation or strain [28], (see Figure 2.8). This phenomenon is studied by applying a constant deformation to the specimen and measuring the stress required to maintain the strain as a function of time. Stress relaxation is known to be temperature, strain rate and load dependent. The majority of material relaxation occurs in the immediate aftermath of the initial strain application in a relatively short time span. Stress relaxation measurements can be made in compression, tension and shear; the first being the most important for the application under discussion here. Stress relaxation is associated with the reorientation of network chains under constant strain, disengagement and rearrangement of chain entanglements and movement of loose chain ends [33].
The processes causing both stress relaxation and creep may be physical or chemical in nature and is usually a combination of both [34]. At lower temperatures and/or shorter time periods, the viscoelastic phenomenon is dominated by the physical processes, whilst at higher temperatures and/or long time periods the chemical processes dominate.

2.6.3 Boltzmann Superposition principle

The Boltzmann superposition principle states that the effect of a compound cause is the sum of the effects of the individual causes. The application of this principle allows the use of a limited amount of experimental data to predict the mechanical response of an amorphous polymer to a wide range of loading conditions [35]. The principle can be used to relate creep and stress relaxation. Boltzmann’s principle provides the possibility to express moduli and compliances in terms of one another even when each is time-dependent. Studies into the relationship between stress relaxation and creep can be, among others, attributed to Leaderman and Ferry [26, 36].
2.6.4 **Hysteresis**

The energy absorbed by a purely elastic material during deformation is stored elastic energy which is wholly released in recovery from the deformation. In a viscoelastic material the storage of elastic energy during deformation is accompanied by energy dissipation due to viscous losses. The phenomenon of energy loss over the deformation load-unload cycle is referred to as hysteresis [37]. In a steady state cyclic deformation the net energy absorbed by the material, which is the area between the loading and unloading curves divided by the area under the loading curve, is known as the hysteresis loss of that particular cycle (see Figure 2.9). Mechanical hysteresis is usually attributed to irreversible thermodynamic changes such as disentanglement of chains, secondary bond breaking and the disruption of hydrogen bonds [12, 32]

![Figure 2.9: Typical loading-unloading curve for polyurethane foam. The area between the loading and unloading curve divided by area under the loading curve, represents hysteresis for this deformation cycle.](image)

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2.6.5 Strain rate dependency

The dependency of polyurethane foam on strain rate is also noticeable upon inspection of material stress-strain curves [38, 39]. Faster strain rates produce higher elastic or shear moduli for FPF [40-42], as illustrated in Figure 2.10.

![Figure 2.10: Strain rate dependence on material modulus](image)

2.6.6 Glass transition temperature, $T_g$

An important parameter for polymer materials is the glass transition temperature ($T_g$), which characterises the transition from the solid to the rubber (or foam) or viscous liquid. For the type of FPF used in this study, the glass transition temperature is between 5 and 25 °C [43, 44]. The glass transition temperature for polyurethane foam materials varies widely, largely depending on the cross-linking density and the molecular weight of the foam [44]. Below $T_g$, the configurations of polymer chain backbones are largely immobilised and the large changes in viscoelastic behaviour associated with time and temperature differences do not appear [45]. Above $T_g$, the polymer is viscoelastic or elastomeric and can sustain large, often recoverable, deformations without true yield or fracture, providing a flexible foam. Increased temperature tends to soften the viscoelastic foam and improves pliability, compression and recovery rate properties [12, 43, 46, 47].
Chapter 3  Modelling polyurethane foam behaviour

This chapter describes a range of methodologies used in the research to model structural and thermal behaviour of polyurethane foam. The hyperelastic behaviour of this material is discussed in more detail in this chapter and Ogden’s Hyperfoam model [48] is presented. Viscoelasticity is also a prevalent characteristic of polyurethane foam. The spring-dashpot models used to model viscoelasticity in FE simulations presented later in this thesis are also presented here. Theories such as the WLF (Williams, Landel and Ferry) [49], TTS (Time-Temperature Superposition) and Arrhenius are also used in the research described later in this thesis, in conjunction with test data, to generate predictions of long-term material behaviour. The theory underpinning these principles is presented here. Polyurethane foam is also a temperature dependent material and thermal conductivity has an important role in any accurate thermal-structural FE model of seating behaviour. The thermal heat transfer behaviour of polyurethane foam is briefly discussed and a model, first presented by Glicksman et al [50, 51], is presented here. Finally the main principles of the Finite Element Method (FEM), which is used throughout the latter stages of this thesis, are discussed in this chapter.

3.1  Hyperelastic model description

3.1.1  Ogden’s Hyperfoam model

The work performed on an elastic material under load is given by the following expression:
\[ dW = \sigma d\varepsilon \] (3.1)

Hence for linear elastic materials, integration of this expression gives:

\[ W = \frac{\sigma \varepsilon}{2} \] (3.2)

which is the potential energy stored per unit volume. The approaches for the
determination of stress-strain behaviour for elastomeric materials can differ in the
derivation and formulation of the strain energy function. Most material models are
based on a mathematical phenomenological approach. It is common that the strain
energy function \( U \), describing elastomers, be specified in terms of the principal
stretch ratios \( \lambda_1, \lambda_2 \) and \( \lambda_3 \) or in strain invariants \( I_1, I_2 \) and \( I_3 \). The stretch
ratio \( \lambda_i \) can be defined as the quotient of the current deformed length \( l_i \) and the
initial length \( l_{0,i} \) in the three main axes:

\[ \lambda_i = \frac{l_i}{l_{0,i}} \] (3.3)

The stretch ratio has the following relationship with \( \varepsilon_i \), the general expression for
nominal strain:
\[
\lambda_i = 1 + \varepsilon_i \quad (3.4)
\]

In terms of the principal stretches of the deformation \(\lambda_1\), \(\lambda_2\) and \(\lambda_3\), the three most common even powered invariants are:

\[
I_1 = \lambda_1^2 + \lambda_2^2 + \lambda_3^2 \quad (3.5)
\]

\[
I_2 = \lambda_1^2 \lambda_2^2 + \lambda_2^2 \lambda_3^2 + \lambda_1^2 \lambda_3^2 \quad (3.6)
\]

\[
I_3 = \lambda_1^2 \lambda_2^2 \lambda_3^2 \quad (3.7)
\]

As \(I_3 = 1\) for incompressible materials, \(U\) is a function of \(I_1\) and \(I_2\). When a material is extended to \(\lambda\) in the x-direction, the true stress \(\sigma_x\), which is computed from \(U\) by differentiation, gives:

\[
\sigma_x = (\lambda^2 - \lambda^{-1}) \left[ \frac{\partial U}{\partial I_1} + \lambda^{-1} \frac{\partial U}{\partial I_2} \right] \quad (3.8)
\]
The first partial differential in the above equation is related to the relative density of network chains per unit volume and can be denoted by the constant \( C_1 \). The second partial differential can be replaced by a constant term \( C_2 \) and this is related to the density of dangling chains (chains that are only connected to the network at one end) [39]. The development of material constants \( C_1 \) and \( C_2 \) from mechanical theory is not covered in this work. The stress-strain relation (Equation 3.9) can then take the form (termed the Mooney-Rivlin equation):

\[
U = c_{10} (I_1 - 3) + c_{01} (I_2 - 3) \tag{3.9}
\]

Each of the material models for elastomers assumes that the material is incompressible. This can be assumed for initial strains because the required forces for isochoric deformation are much smaller than those causing a reduction in volume. Kinematically, an incompressible material is represented by assigning a Poisson’s ratio \( \nu \) of approximately 0.5. Incompressibility is imposed by the constraint:

\[
J = \lambda_1 \lambda_2 \lambda_3 = 1 \tag{3.10}
\]

Where \( J \) is the volume ratio, which is the ratio of the current volume to the reference volume.
Principal stretch based models have also been proposed by some authors. They are simpler to implement than invariant based models. Ogden [52] proposed a principal stretch based model, Equation 3.11:

\[
U = \sum_{i=1}^{N} \frac{2\mu_i}{\alpha_i} \left[ \hat{\lambda}_i^{\alpha_i} + \hat{\lambda}_2^{\alpha_i} + \hat{\lambda}_3^{\alpha_i} - 3 \right] + \sum_{i=1}^{N} \frac{1}{D_i} (J_i^{el} - 1)^2 \quad (3.11)
\]

Where \( \hat{\lambda}_i \) are the deviatoric principal stretches, \( \hat{\lambda}_i = J^{\frac{1}{3}} \lambda_i \) and \( \lambda_i \) are the principal stretches, \( N \) is the order of fitting; \( \mu_i, \alpha_i \) and \( D_i \) are temperature dependant material parameters and \( J_i^{el} \) is the elastic volume ratio.

The material model was then extended to cater for compressible materials, in which large volumetric changes are accommodated [48, 53]. This model has an extra term added to the incompressible Ogden model to allow for compressibility and is known as the Ogden Hyperfoam model:

\[
U = \sum_{i=1}^{N} \frac{2\mu_i}{\alpha_i} \left[ \hat{\lambda}_i^{\alpha_i} + \hat{\lambda}_2^{\alpha_i} + \hat{\lambda}_3^{\alpha_i} - 3 \right] + \frac{1}{\beta_i} \left[ (J_i^{el})^{-\alpha_i\beta_i} - 1 \right] \quad (3.12)
\]
where $U$ is the strain energy per unit volume, $N$ is the order of fitting; $\mu_i$, $\alpha_i$ and $D_i$ are temperature dependant material parameters. $J^{el}$ is the elastic volume ratio and $\lambda_i$ represents principal stretch ratios:

$$\lambda_i = \left(J^{th}\right)^{\frac{1}{3}} \rightarrow \lambda_1\lambda_2\lambda_3 = J^{el} \quad (3.13)$$

where $J^{el}$ is the elastic volume ratio and $J^{th}$ is the thermal volume ratio, defined in Equation 3.14 as:

$$J^{el} = \frac{J}{J^{th}} \quad (3.14)$$

where $J$ is the total volume ratio.

The initial shear modulus, $\mu_0$, is related to coefficients, $\mu_i$, by the expression:

$$\mu_0 = \sum_{i=1}^{N} \mu_i \quad (3.15)$$

and the initial bulk modulus, $K_0$, follows from:
\[ K_0 = \sum_{i=1}^{N} 2\mu_i \left( \frac{1}{3} + \beta_i \right) \]  

(3.16)

\( \beta_i \) is then related to Poisson’s ratio \( \nu \), as shown in Equation 3.17:

\[ \beta_i = \frac{\nu}{1 - 2\nu} \]  

(3.17)

The thin cell-wall structure of the type of foam used in this research allows wall buckling under pressure without lateral resistance/constraints, meaning that principal stresses/strains are assumed to be fully de-coupled. Additionally the transverse stretches, \( \lambda_1 \) and \( \lambda_3 \), were noted as being negligible during axial compression testing, suggesting that an assumption of Poisson’s ratio \( \nu = 0 \) was valid. As a result of this approximation, \( \beta_i \) which controls the degree of compressibility of the material (Equation 3.17), is assumed to be zero. These assumptions have been used for polyurethane foam previously by different authors [39, 54-57]. Equation 3.12 can thus be simplified to allow calculation of the nominal stress, \( T_2 \) in the \( \lambda_2 \) load direction using the following equation:
\[ T_2 = \frac{\partial U}{\partial \lambda_2} = 2 \sum_{i=1}^{N} \mu_{i} \lambda_{2}^{\alpha_{i}} - J_{el}^{-\alpha_{i}\beta_{i}} \] (3.18)

A nonlinear least squares regression method is used to fit the model to test data [58].

### 3.1.1 Use of the Ogden Hyperfoam model

This model is available in most commercial FE packages including Abaqus [2] and has been used by several different authors. Schrod\(t\) and Sil\(b\)er conducted indentation modelling with specially optimized parameters [59, 60]. The analyses described here use different test methods to develop material model parameters capable of predicting stress distributions throughout an indented foam sample [61, 62]. The use of the inverse FE modelling method by Li et al is an alternative method suited to the challenge of modelling indented polyurethane foam [63].

Research was conducted by Mills and co-workers assessing the usefulness of this type of foam for different personal protection equipment applications. Ogden’s strain energy function was also used to analyse the behaviour of personal protection equipment such as running shoe soles and crash helmets [64-68]. Widdle et al modelled the behaviour of polyurethane foam using Ogden’s model and took into account the influence of Poisson’s ratio on results [69, 70]. Gruijicic et al [54], Silber et al [71] and Tang et al [72] have all used Ogden’s model to represent foam in automobile seat-passenger simulation models.
3.2 Viscoelastic model description

Viscoelastic theories are commonly described using mathematical models of material behaviour [26]. Viscoelastic behaviour is usually modelled using combinations of springs and dashpots [45]. When using spring and dashpot models, the parameters used are not linked to polymer microstructure or to the foam structure [39]. The simplest mechanical model for modelling material behaviour is a pure Hookean spring. Hooke’s law, Equation 3.19, relates $\sigma$ and $\varepsilon$ linearly, with the proportionality constant, $k$, being Young’s modulus for the material:

$$\varepsilon = \sigma / k$$  \hspace{1cm} (3.19)

The dominant characteristic when modelling fluid behaviour is its viscosity. Simple linear viscous behaviour of fluid is modelled using Newton’s equation of motion, Equation 3.20:

$$d\varepsilon / dt = \sigma / \mu$$  \hspace{1cm} (3.20)

Physical spring dashpot models, capable of modelling linear viscoelastic effects, can be constructed from simple elements such as the elastic spring and the viscous dashpot, where the spring of modulus $k$ is assumed to comply with Hooke’s law and the dashpot is assumed to be filled with a Newtonian fluid of viscosity $\mu$. 
3.2.1 Maxwell Model

![Figure 3.1: Maxwell Model spring-dashpot arrangement.](image)

The stress-strain relationships for the spring and dashpot components are of the forms described in Equations 3.19 and 3.20. The Maxwell model consists of a spring and dashpot in series. The total strain across this model must equal the sum of the strains of both components, Equation 3.21:

$$\varepsilon = \varepsilon_1 + \varepsilon_2$$  \hspace{1cm} (3.21)

and the strain rate across the model is then given by Equation 3.22:

$$\frac{d\varepsilon}{dt} = \frac{d\varepsilon_1}{dt} + \frac{d\varepsilon_2}{dt} = \frac{1}{k} \frac{d\sigma}{dt} + \frac{\sigma}{\mu}$$  \hspace{1cm} (3.22)

For creep at constant stress $\sigma = \sigma_0$, Equation 3.22 thus reduces to:

$$\frac{d\varepsilon}{dt} = \frac{\sigma_0}{\mu}$$  \hspace{1cm} (3.23)
Integrating Equation 3.23, gives Equation 3.24:

$$\varepsilon(t) = \frac{\sigma_0}{\mu} t + C_i$$  \hspace{1cm} (3.24)

where the constant of integration, $C_i$, is found from the initial condition $\varepsilon(0) = C_i = \sigma_0 / k$. Thus, creep strain for the Maxwell model is given by Equation 3.25:

$$\varepsilon(t) = \frac{\sigma_0}{\mu} t + \frac{\sigma_0}{k}$$  \hspace{1cm} (3.25)

The equation for creep compliance, $J(t)$, is displayed below:

$$J(t) = \frac{\varepsilon(t)}{\sigma_0}$$  \hspace{1cm} (3.26)

Therefore the creep compliance corresponding to a creep strain in a Maxwell element is given by Equation 3.27:
\[
J(t) = \frac{\varepsilon(t)}{\sigma_0} = \frac{t}{\mu} + \frac{1}{k}
\]  
(3.27)

For relaxation at constant strain \( \varepsilon = \varepsilon_0 \), the Maxwell model stress-strain relationship in Equation 3.22 becomes:

\[
0 = \frac{1}{k} \frac{d\sigma}{dt} + \frac{\sigma}{\mu}
\]  
(3.28)

Integrating, we find that:

\[
\ln \sigma = -\frac{k}{\mu} t + C_2
\]  
(3.29)

where the constant of integration, \( C_2 \), is found from the initial condition \( \sigma(0) = \sigma_0 \). The resulting stress relaxation function is:

\[
\sigma(t) = \sigma_0 e^{-kt/\mu} = \sigma_0 e^{-t/\lambda}
\]  
(3.30)

where \( \lambda = \mu/k \) is the relaxation time, whose corresponding relaxation modulus is:
The Maxwell model is capable of reasonably accurately modelling relaxation, but not creep behaviour.

3.2.2 Kelvin-Voigt model

A Kelvin-Voigt model, Figure 3.2, consists of a spring and dashpot in parallel.

Using the appropriate equations for a parallel arrangement, total stress across the system must equal the summation of the stress in the spring and in the dashpot. It can be shown using a similar procedure to that used in section 3.2.1 that:

\[
\sigma = k\varepsilon + \mu \frac{d\varepsilon}{dt} \quad (3.32)
\]

It can also be shown that the creep compliance for the Kelvin-Voigt model is given by Equation 3.33:
\[ J(t) = \frac{1}{k} \left[ 1 - e^{-t/\rho} \right] \]  

(3.33)

Where \( \rho = \mu / k \) is now referred to as the retardation time. Similarly, the relaxation modulus is given by Equation 3.34:

\[ C(t) = k \]  

(3.34)

The Kelvin-Voigt model cannot model relaxation, as it assumes a constant stress throughout. Thus, like the Maxwell model, the Kelvin-Voigt model cannot accurately represent all aspects of viscoelastic behaviour.

3.2.3 Zener model

The standard linear or Zener model improves on the standard Maxwell model by adding a spring in parallel with the system, as displayed in Figure 3.3:

\[ \text{Figure 3.3: Zener spring-dashpot arrangement} \]

The creep compliance of a Zener model is given by Equation 3.35:
\[ J(t) = \frac{1}{k_0} \left[ 1 - \frac{k}{k_0 + k_i} e^{-t/\rho_i} \right] \]  

(3.35)

where \( \rho_i = \frac{\mu_i}{k_0 k_i} (k_0 + k_i) \) is the retardation time.

The relaxation modulus of the Zener model is calculated using Equation 3.36:

\[ C(t) = k_0 + k_i e^{-t/\lambda_i} \]  

(3.36)

where \( \lambda_i = \mu_i / k_i \) is the relaxation time.

A Zener model can predict stress relaxation and creep behaviour moderately, but cannot model long-term behaviour accurately as it is a first order model. Although an improvement on both the Maxwell and Kelvin-Voigt models, the Zener model is not a complete representation of viscoelastic behaviour. The model tends to predict complete relaxation as occurring quite early in the time-scale, contrary to what has experimentally been observed for polymers. Improved versions of the Zener model may be achieved by connecting additional Maxwell elements parallel to a spring or connecting additional Kelvin-Voigt models in series with a spring.

### 3.2.4 Improved Zener Models

By using improved versions of the Zener model it is possible to extend accurate prediction of relaxation to more realistic times. A combination of multiple
Maxwell elements in parallel with a spring (known as a Maxwell-Wiechert model), is presented in Figure 3.4:

![Diagram of Maxwell elements in parallel with a spring](image)

**Figure 3.4:** Improved Zener model (parallel arrangement); multiple Maxwell elements in parallel with a spring.

It can be shown that the relaxation modulus for the Maxwell-Wiechert model is given by:

\[
C(t) = k_0 + \sum_{i=1}^{n} k_i e^{-t/\lambda_i}
\]  
(3.37)

where \( \lambda_i = \mu_i / k_i \) is the retardation time for the \( i^{th} \) Maxwell element.

An alternative form of an improved Zener model, consisting of a spring in series with a number of Kelvin-Voigt elements, is shown in Figure 3.5.
Figure 3.5: Improved Zener model (series arrangement); multiple Kelvin-Voigt elements in series with a spring.

It can be shown that the corresponding creep compliance for this model is given by Equation 3.38:

\[
J(t) = \frac{1}{k_0} + \sum_{i=1}^{g} \frac{1}{k_i} \left[ 1 - e^{-t/\rho_i} \right]
\]  

(3.38)

where \( \rho_i = \mu_i / k_i \) is the retardation time for the \( i^{th} \) Kelvin-Voigt element.

Improved Zener models are used later in this thesis, along with suitable test data and behavioural predictions, to model long-term material behaviour.

3.2.5 Viscoelastic modelling of foam

Viscoelasticity is generally modelled using some combination of the spring-dashpot models described so far in section 3.2. Unknown coefficients are usually obtained by curve-fitting the chosen model to creep, stress relaxation or frequency dependent test data. Several researchers have proposed different methods for modelling the strain rate dependency associated with viscoelasticity [57, 73-76]. Zhang \textit{et al} proposed a model capable of predicting temperature and strain rate dependence in polypropylene foam materials [57]. The linearity of polypropylene
strain rate dependence allowed the model to work successfully. Ramon et al created an accurate strain rate dependent model of a type of polyurethane foam which produced a first order stress-strain compression test result configuration. Both of these materials displayed near-linear behaviour that was easier to model than the materials that were tested in the work described here. A model developed by Ehlers et al [77] used the theory of porous media to model viscoelastic behaviour of FPF. This method identified parameters with data extracted from frequency vibration tests. Briody et al [61, 62], Mills et al [78] and Gruijic et al [54] attempted to model viscoelasticity using the Maxwell-Wiechert model and a Prony Series of material constants.

3.3 Standard long-term prediction methods

3.3.1 Time-Temperature superposition principle

Ideally, studies involving the creep compressive behaviour of polymers should be carried out over long time periods at the expected in-service (often ambient) temperatures and stress levels. However, for many reasons, including the time required to conduct long-term tests, this is often not possible. Two accelerated test methods exist, one using the Time-Temperature Superposition (TTS) principle and the other using the Time-Stress Superposition (TSS) principle, which are utilised to generate predictions of long-term creep behaviour. TTS relies on the fact that higher temperatures accelerate viscoelastic phenomena within viscoelastic materials, while the basis for TSS is that higher stresses accelerate viscoelastic phenomenon. Some authors have used TTS, independently proposed by Leaderman, Tobolsky and Andrews [36, 79, 80], to study viscoelastic creep and viscoelastic stress relaxation in rigid polymers [81-83]. The TTS method was
applied, in this study, to facilitate the characterisation of the long-term behaviour of viscoelastic polyurethane foam. Other researchers have used TSS to obtain long-term data from short term creep tests with different applied stresses [84-86].

In Chapter 7 of this thesis, TTS will be applied to creep compression test results; generating a list of shift factors and a creep master-curve. The theory behind TTS states that a shift factor, \( a_T \), at any temperature, is the ratio between the time for a viscoelastic creep process, \( \lambda_i \), to proceed at the test temperature, \( T \), and the time for the same process to proceed at a reference temperature, \( T_r \), as given in Equation 3.39:

\[
\lambda_i(T) = a_T \lambda_i(T_r)
\]  

(3.39)

### 3.3.2 WLF equation

Williams, Landel and Ferry [49] developed an empirical formula (Equation 3.40) describing the time-temperature relationship through the shift factor, \( a_T \):

\[
\log a_T = \frac{-c_1(T - T_r)}{(c_2 + T - T_r)}
\]

(3.40)
where $C_1$ and $C_2$ are material dependent parameters which are a function of material type and reference temperature, $T_r$. Equation 3.40 does not hold below $T_g$ since it predicts a monotonic increase of $\log a_T$ with decreasing temperature, meaning the inflection point which exists near $T_g$ is not accounted for. The empirical formula is expected to be accurate for this material within the temperature range $T_g$ to $T_g + 100°C$ [49]. The application of WLF is common in long-term prediction of polymer behaviour [87]. In this thesis, the first insight into the applicability of this behaviour for FPF material was provided [88].

### 3.3.3 The Arrhenius method

The Arrhenius principle describes the relationship between the rate of a chemical reaction during a test and the test temperature. Exposure of test samples to a series of elevated temperatures, leads to the development of a relationship between temperature and the reaction rate of degradation mechanisms. Arrhenius’ equation represents this relation:

$$\ln k(T) = \frac{-E_a}{RT} + C$$  \hspace{1cm} (3.41)

where $k(T)$ is the reaction rate, $C$ is a constant, $E_a$ is the activation energy (KJ/Mol), $R$ is the gas constant (J/Kmol) and $T$ is the absolute temperature (K).

Testing is conducted until the relevant threshold value of creep has been exceeded, so that the time-limit for that threshold and temperature combination
may be determined. Testing should be conducted at three or more additional temperatures. The time limits obtained from each temperature test are plotted on an Arrhenius chart as a function of temperature. The straight line through a set of creep threshold points may be extrapolated back to generate Arrhenius based creep predictions at the temperature of use, i.e. the service temperature. The Arrhenius model is based on the chemical degradation of material under loading; it can be used to comprehend the relation between the physical and chemical components of long term material behaviour.

3.4 Modelling thermal conductivity of polyurethane foam

The total heat transfer through a sample of polyurethane foam is the sum of the conductivities of the gas and solid plus the thermal conductivity due to radiation. Polyurethane has a density of 1200 kg/m$^3$ and the FPF studied in this work had density values ranging from 40 kg/m$^3$ to 85 kg/m$^3$. The relative density, which is the sample density divided by the constituent material density, is quite low, ranging from 0.033 to 0.071. Conduction is the main form of heat transfer through a foamed material; therefore the thermal conductivity of FPF is relatively low, as the amount of material available to conduct heat is relatively low.

Heat transfer through thermal insulation material has been widely studied [50, 51, 89-91]. Much of the work has been conducted to help understand behaviour of closed cell foam heating insulation. Glicksman et al have developed equations, based on physical material attributes, predicting heat transfer through different types of foams [50, 51]. These equations were utilised more recently, with successful results, to model thermal behaviour of piping insulation material [92]. Thermal conductivity is a temperature dependant property and its dependence has
been found to be similar for all foams over a range of 10°C to 40°C [92]. These equations accounted for conduction through the stagnant gas, conduction along the solid polymer material and thermal radiation of the solid-gas medium. The three modes of thermal conductivity are each represented by thermal conductivities, whose sum combine to give an effective total heat transfer, \( K \), for a closed cell foam which is assumed to correspond to Equation 3.42 [93]:

\[
K = k_S + k_G + k_R \quad (3.42)
\]

Where \( k_S \) is the heat conductivity due to the solid constituent material of the cell walls, \( k_G \) is the conductivity of the cell gas mixture and \( k_R \) is the radiation between cells.

Sinofsky also developed an equation to allow for the calculation of an effective open-celled foam heat transfer, \( k_{\text{eff}} \) [93]. The open-celled polyurethane foam material, which is utilised in seating, displays substantially different thermal conductivity characteristics to closed-cell materials. For open-celled material, such as the material being studied in this work, the \( k_G \) term is assumed zero as the material has no gas trapped within its cellular structure.
\[
 k_{\text{eff}} = \left( \frac{\% \text{ o. c.}}{k_R + k_S + k_A} \right)^{-1} \quad (3.43)
\]

where \% o. c. refers to the percentage of open cells within the structure, which
is assumed to be 100% for the seating materials being used and \(k_a\) is the
conductivity value for air which increases as temperature rises.

The thermal conductivity heat transfer component, \(k_S\), was calculated using
Equation 3.44 [93]:

\[
 k_s = \lambda_{PU} \left( \frac{1}{3} \right) f_s (1 - \delta) + \lambda_{PU} \left( \frac{2}{3} \right) (1 - f_s)(1 - \delta) 
\quad (3.44)
\]

Where \(\lambda_{PU}\) is the thermal conductivity of the constituent material and \(f_s\) is the
fraction of solid in the cell struts. The term \(\delta\) is calculated using Equation 3.45:

\[
\delta = \left( 1 - \frac{\rho_f}{\rho_s} \right) \quad (3.45)
\]

where \(\rho_f\) is foam density and \(\rho_s\) is solid (constituent material) density.
The radiation component, $K_R$, is calculated using Rossland’s equation with a cell wall extinction co-efficient (measurement of radiation transmission through a material sample), $K_{w}$, of 60,000$^{-1}$ proposed by Sinofsky for polyurethane foam [93]:

$$K_r = \frac{16BT^3}{3K}$$  \hspace{1cm} (3.46)

where $B$ is the Boltzmann constant, $T$ is the temperature (°K) and the extinction coefficient can be calculated using the equation:

$$K = \left(4.10\sqrt{\frac{f_s\rho_f}{\rho_s}}\right) + \left(\frac{1 - f_s)\rho_f}{\rho_s}\right)K_W$$  \hspace{1cm} (3.47)

where $d$ signifies the average cell diameter.

The insulating nature of the polyurethane foam material meant that thermal conductivity values were not easily attainable, as during measurement, heat loss was difficult to measure. Equation 3.43 was therefore used to generate values for foam conductivity. These foam conductivity values were used in simulations of material behaviour under typical seating temperatures. Accurate conductivity
values were important to ensure accurate model results as seating temperatures, especially wheelchair seating temperatures, play a critical role in seating performance [94, 95].

3.5 The Finite Element Method

The Finite Element Method (FEM) is a numerical analysis technique for obtaining approximate solutions to a wide variety of engineering, mathematical and scientific problems. The method has become popular as continuous advances in computational power combined with improvements in commercial FE packages lead to faster and more accurate solutions. Most common engineering problems are defined on geometrically complex domains having varying boundary conditions. Therefore, it is often very difficult to find an analytical solution or to generate approximation functions using traditional methods. The basic idea of the FE method is to break up any complex domain into a discrete number of smaller "elements". A given domain can then be viewed as an assemblage of simple geometric finite elements, for which it is possible to systematically generate approximation functions of domain behaviour.

3.5.1 Discretisation and shape functions

To discretise a continuum structure, its domain is split into a discrete number of smaller elements. The type of element chosen depends on the dimensionality and the general shape of the structure. One-dimensional structures will require one-dimensional elements, this pattern is repeated for two and three-dimensional structures. A structure with higher dimension elements will have a higher amount of nodes. The combination of nodes that cover the structure is known as the mesh. Reducing the space between nodal points increases the mesh density. Increasing
mesh density can have the effect of increasing accuracy in important areas of a component, but comes with increased computational time. Approximation functions are generated across the assemblage of finite elements, these approximation functions are known as shape functions. Each shape function can be any mathematical formula which helps to interpolate and approximate values at points in between the mesh nodal points.

3.5.2 Element Types

Each element is constructed using two or more nodes as the end/corner points. The simple framework element is used in one-dimensional analyses, often acting as a truss or a beam in a space frame structure. These elements can also be used in two or three-dimensional analyses often intended as a stiffening member or a spring. Thin plate elements are loaded by forces in their own plane (plane stress condition) and are often referred to as basic elements. Triangular and quadrilateral elements are the two most common thin plate elements, with applications in a wide range of two-dimensional design. Solid elements are three dimensional generalisations of two dimensional elements. Tetrahedron and hexahedron are the most commonly used solid elements. Element types can be linear or quadratic, with linear elements having less nodal points meaning that they are generally not as accurate but are much less computationally expensive. Other types of elements used in FEA are axisymmetric solids, axisymmetric thin shells, thin flat plate elements for bending and curved thin shell elements. These elements types were not utilised in any part of this work.

3.5.3 Finite element analysis procedure

Each FE analysis went through the following broad steps:
- Discretisation of the domain into a set of finite elements (mesh generation).
- Formulation of the differential equation over a typical element.
- Assembly of finite elements to obtain the global system of algebraic equations.
- Imposition of essential boundary conditions.
- Solution of the system of algebraic equations to find (approximate) values.
- Post-computation of solution and extraction of quantities of interest.

3.5.4 Nonlinearity

Many structural problems analysed using finite elements are linear. However, there are many systems where a linear equation is inadequate. The main causes of nonlinearity are geometrical nonlinearities, changing status (contacting components) and material nonlinearities.

**Geometrical nonlinearities**

Geometrical nonlinearities occur when the loads applied produce large deflections which significantly alter the geometry of the problem. This change in geometry can possibly affect structure stiffness and the manner in which loads are applied. Such problems are normally solved by a gradual application of the load in a number of smaller steps. Equilibrium is calculated iteratively after each step using methods such as the Newton-Raphson method.

**Change of status nonlinearity**

The most basic example of this kind of nonlinearity is a rope, which has stiffness in tension but none in compression. A commonly encountered example of this type of nonlinearity is contact, which is a common occurrence in most assembly type FE analyses. The nonlinearity of a system in contact arises from the fact that
status of contact is not known prior to the analysis. Complications arise as the change of status creates large changes in the stiffness matrix and load depending on the contact status, whether or not components are touching or separated. Contact problems often involve other nonlinearities such as large deflection and material nonlinearities. Several modes of contact, such as bonded, frictionless, rough and frictional, can be defined within an FE problem. These modes of contact are defined with specific contact algorithms which themselves have different controlling parameters. Common algorithms include Lagrange, MPC and penalty stiffness.

**Material nonlinearities**

When material behaviour cannot be modelled using a straight line stress versus strain curve, the material is referred to as nonlinear. Elasto-plastic materials which incorporate yielding and plastic behaviour and hyperelastic materials such as polymers (including foams) and human tissue are common nonlinear materials. Crushing, cracking, temperature dependency and viscoelasticity are common phenomena which can cause additional material nonlinearity. When nonlinear material behaviour is expected it is common to model this behaviour using a suitable material model. Most FE packages include curve fitting tools capable of fitting material test data, from one or more test modes, to a wide variety of existing nonlinear models. The user can also specify an appropriate set of material model parameters for an existing model or upload a new model using a user subroutine.
3.6 Summary

A full range of relevant material behaviour can be modelled using the models presented here. A description of the background theory of all models is also given. Parameters are required for each model; these parameters are generated using a combination of material test results and inspection procedures. The generation of parameters is described in Chapter 7 of this thesis. All parameters were verified using a range of procedures which are presented in Chapter 8.
Chapter 4 Polyurethane foam in wheelchair seating

This chapter begins by giving an overview of the use of polyurethane foam in seating, with particular focus on wheelchair seating. Pressure ulcers, which are a regular occurrence and therefore a major problem for wheelchair users, will be introduced. Some discussion on the factors involved in the formation of pressure ulcers will then be presented. Finally, interface pressure mapping, which is commonly used to assist in wheelchair cushion prescription, is discussed.

4.1 Polyurethane foam in seating

The structurally flexible nature of FPF dictates that it is commonly used in all aspects of seating. The comfort and support offered by this flexibility results from a combination of the material’s cellular structure and the rubberlike properties of the constituent polymer. Other aspects influencing the popularity of this material are its ease of manufacture and its low cost. In specialist wheelchair seating, where many users may have limited mobility, the primary objective of the cushioning system is to alleviate areas of high pressure. To achieve this goal, current specialised solutions include cushions with layers of different materials and contoured cushions. The materials studied in this work are classified by their manufacturers as either viscoelastic polyurethane foam or polyurethane foam. Although all foam is viscoelastic to some extent, some polyurethane foam demonstrates increased viscoelastic properties through the inclusion of additives, increasing viscosity and density in the manufacturing process. This type of foam is commonly referred to as ‘memory’ foam; its viscoelastic nature provides improved recovery, resilience and hysteresis properties [96]. Widely used in bedding and clinical seating applications, the material offers excellent comfort and
support for a number of reasons, including its additional time and temperature dependent properties. These properties cause the material to conform to the body’s shape over time as the body’s warmth heats the cushioning material. Increased temperature tends to soften viscoelastic foam and improves pliability, compression and recovery rate properties [12, 43, 47, 97]. Areas of high pressure are also where most heat is conducted to the cushioning material; this increases the effectiveness of the material in these areas [96]. Manufacturing processes can be tailored to increase the spectrum of operating temperatures of viscoelastic polyurethane foam. These processes have proven useful in seating applications [98]. Conventional polyurethane foam does display some viscoelastic properties during use; however these properties are not as pronounced as in viscoelastic foams.

4.2 Introduction to pressure ulcers

A pressure ulcer can be defined as a localised injury to the skin or underlying tissue, usually over a bony prominence (such as the Ischial Tuberosities), as a result of pressure, or pressure in combination with shear or friction [99]. Pressure ulcers have the potential to interfere with physical, psychological and social well-being and to impact overall quality of life [100]. Ulcer development occurs in two forms, superficial and Deep Tissue Injury (DTI). Figure 4.1 displays MRI readings of the internal changes in the trunk sitting region due to loading. The loaded IT (right) is closer to the seating interface than the unloaded IT (left), meaning that the skin is under increased pressure. Ulceration will occur if this load is applied over extended periods without movement. Immobility, postural changes, progressive damage of the gluteal and posterior thigh muscles,
incorrectly adjusted footplates and altered sitting postures resulting from orthopaedic surgery can all effect and increase pressure during sitting [101].

Figure 4.1: Experimental setup for recording MRI images from buttock–thigh area in a simulated sitting posture with simulated sitting load applied. Top: MRI setup to measure buttock–thigh structure under two loading configurations (Left: without sitting pressure; Right: with sitting pressure) for the simulated sitting posture. Bottom: corresponding MRI images. Adapted from Makhsous et al [102].

4.2.1 Skin – tissue layer structure

Healthy tissue such as that shown in Figure 4.2 consists of the epidermis, dermis, adipose tissue, muscle and bone. The epidermis is the tough outer layer of skin meant for the protection of internal organs. Underneath the epidermis is the dermis, which is a layer of highly elastic flexible skin containing nerves, lymphatic components and blood vessels. Between the dermis and muscle lies the subcutaneous tissue (adipose tissue), which provides cushioning, energy storage and protection for the muscle. The muscle which lies next to the bone contains layers of fibres, which are controlled by the nervous system. As each layer has
different mechanical properties, each has different resistance to pressure and shear forces. In general, muscle tissue is most affected by excessive pressure. Damage of the muscle is more likely than damage of any other layer [103].

Figure 4.2: Healthy tissue layers (not to scale), NPUAP [99].

4.2.2 Types of pressure ulcers

Superficial ulcers affect the outer layers of skin near epidermal tissue and they are typically associated with the presence of moisture and heat along with frictional and shear forces. Generally the extent of a superficial pressure ulcer is not as serious as a DTI [1, 3, 4, 104]. The damage is mostly reversible since it is contained within the top layers of regenerative tissue [3, 104].

DTI are different to superficial pressure ulcers in their mechanisms and in the nature of the damage they cause. Ulceration begins on the muscle which surrounds bony prominences during sustained periods of compression. These ulcers are harmful and develop faster than superficial ulcers. As the damage progresses towards the surface, damage of the muscle, fascia and subcutaneous tissue may occur. This may go unnoticed from visual inspection by the practising
care-giver. The initial pathological changes which lead to the most severe pressure ulcers are therefore difficult to identify [1].

4.3 Formation of pressure ulcers

Given current evidence, using suitable support surfaces, repositioning the patient and optimizing nutritional status are appropriate strategies to prevent pressure ulcers [105]. The factors relating to the development of pressure ulcers can be separated into extrinsic and intrinsic factors. Intrinsic factors are related to the subject’s age profile as well as the nature and severity of the clinical condition. Extrinsic factors are ones that can be controlled to a certain extent, such as the mechanical load (pressure and shear) and environmental effects such as temperature and humidity [106]. The focus of this section is on the extrinsic factors and how they contribute to the mechanisms involved in pressure ulcer formation.

Pressure ulcer occurrence is predominantly a problem for physically and neurologically disabled individuals who often suffer from a wide range of diseases. These disabilities create a set of factors that cause an increased chance of ulceration. Reasons for increased chance of pressure ulcer occurrence within the paraplegic population include: lack of position changing during sitting [107]; higher compressive and shear strains in the gluteus muscle [107, 108]; increased heat build-up [109] and sensory defects [110].

4.3.1 Pressure

The prevalence of pressure ulcers amongst wheelchair users, as well as in subjects who are bed-ridden, remains unacceptably high. Bouten et al expressed the opinion that the prevalence of pressure ulcers in the paraplegic population is
partly due to limited fundamental knowledge of the physical development of the clinical condition [1]. Pressure ulcers unquestionably result from a very complex set of risk factors, of which direct pressure and the associated loss of circulation are critical. Reduced tissue perfusion occurs as a result of this associated loss of circulation. This pressure, coupled with the reduction in perfusion, results in increased risk of a wheelchair user developing a pressure ulcer [111]. Clinical tests, carried out by Peterson et al on over 1000 spinal injury patients, found that pressure of 5.3kPa or under in the ischial tuberosities region is usually safe if the patient is able to shift their bodyweight to relieve pressure during long periods of sitting [101]. Classically, it had been postulated that internal blood vessel pressure was demonstrated to be in the range of 2.66 - 4kPa [112]. Interfacial pressures over this threshold were therefore assumed to cause tissue breakdown due to vessel closure. However, vessel closure depends on local pressure gradients across the vessel wall and not just on the pressure between the skin and support surface. Therefore interface pressures above capillary pressures can be supported by soft tissues before blood flow is impaired by capillary occlusion [1, 113]. The design and application of preventive aids and risk assessment techniques are dominated by subjective measures backed up by some quantitative data, such as that quoted in this section.

The pressure-time relationship is an important factor in the development of pressure ulcers. In a classic study [114] Kosiak demonstrated that an inverse relationship was shown to exist between the amount and duration of pressure required to render pathological changes in animal tissues. Reswick and Rogers (Figure 4.3) [115] have accumulated more realistic data from actual patient experience which confirms the shape of the pressure-time curve produced by
Kosiak but has a different gradient. Reswick and Rogers’ curve suggest that a pressure of 5.3kPa would have an approximately 50% chance of causing an ulcer if imposed for 8 hours. If that pressure was held for periods longer than 8 hours, there would be a higher than average probability of tissue breakdown, whereas if the pressure was held for less than 8 hours the probability of tissue breakdown would be lessened. Dharmarajan et al later found that muscle damage can occur when experiencing pressures exceeding 7.9 kPa for more than an hour [116].

![Image of graph showing pressure-time relations]

**Figure 4.3: Pressure-time relations as formulated by Reswick and Rogers [115].**

### 4.3.2 Shear

Shear loads are caused by two phenomena working in combination: pressure and friction [117]. Parallel shear stress occurs when two forces are in opposite directions such that there is a deformation, or attempted deformation, of the tissue in parallel planes. Parallel shear stress is proportional to static friction prior to movement and dynamic friction during movement and proportional to normal force prior to and during movement. Shear also occurs when two forces act in the same direction but with different magnitudes, known as pinch shear. Pinch shear
stress has been defined as ‘the shear stress component acting perpendicular to the skin, which is generated by the non-uniformity of the pressure distribution’ [118]. When forces rapidly go from a high to a low normal force, there is a high normal force gradient (high pinch shear stress). If the force change is more gradual, then there is a lower normal force gradient (low pinch shear stress). Shear force is an important component of the mechanical load in seating and the coefficient of friction is the proportionality constant which relates it to normal force. Excessive shear force leads to a reduction of blood flow, which is widely regarded as one of the most important factors in the formation of pressure ulcers [111]. Shear has been shown in both simulations and tests to have an important influence in the reduction of blood flow through the sacral region [6, 119]. It has also been shown that different combinations of pressure and shear (for example high shear and medium pressure or high pressure and medium shear) when applied to the outside of the skin, still have the same effect beneath the skin. In this way it was demonstrated that not only direct pressure relates to ulceration but also shear stress [6, 120, 121]. Superficial pressure ulcers begin in the outer layers of skin with maceration and detachment of the superficial layers. They are assumed to be generally caused by a combination of mostly shear combined with some direct pressures applied to the skin during sitting [1, 122]. In personalised pressure relieving seating solutions (contoured cushions), which are commonly required for Spinal Cord Injury (SCI) wheelchair users, Brienza found that shear was a large causative factor of superficial pressure ulcer formation [123].

4.3.3 Environmental effects - temperature

The pressure-time-temperature relationship is a crucial factor in ulcer development. An increase in temperature in the trunk region is known to cause an
increase in metabolic demand equivalent to 10% per °C [124, 125]. For this reason, excessive body heat build-up in the body-cushion interface region is often undesirable in wheelchair cushions, as nutrients are expended at a faster rate. Foam is also a poor conductor of heat and can cause excessive heat build-up in wheelchair seating as body warmth is slow to escape [126]. However, a balance in temperature is required as a specific temperature increase is needed to soften the foam sufficiently so that it can provide adequate comfort. If this temperature is not reached it may result in foam rigidity, due to polymer chain back-bone immobility [26], which can reduce the potential for pressure relief. Hence, careful control of temperature is required to help reduce tissue damage. The clinician needs to take this information into account when fitting a customised cushioning system; this adds further complications to the seating prescription process [124].

The development of pressure ulcers has been shown to initiate from a prolonged unrelieved pressure, which results in a subsequent reduction of perfusion and supply of nutrients to local cells [127]. Perfusion data collected by Kokate et al [128] and assessed by Lachenbruch et al [94] demonstrated a relationship between skin temperature and interface pressure. From analysing this skin perfusion data, the effect of cooling the skin from 36°C to 28°C, is estimated to be equivalent to reducing the interface pressure from 7.47kPa to 5.33kPa; which is a reduction of 29%. Lachenbruch then developed a theoretical pressure-time-temperature relationship which included the curve developed by Reswick and Rogers with the temperature-based findings of Kokate et al represented by the raised and lowered threshold curves, shown in Figure 4.4. Pressure-time-temperature readings above the curves indicate a higher probability of pressure ulcer occurrence; whereas values below the curves suggest a lower probability of pressure ulcer occurrence.
For example, a pressure of 10,000 Pa, applied for 20,000 seconds to skin at 4°C warmer than average skin, has a high probability of causing a pressure ulcer. Conversely, a pressure of 6,000 Pa applied for 20,000 seconds to 4°C cooler than average temperature skin, has a low probability of pressure ulcer formation. Tzen et al conducted similar studies which also suggested that local cooling may protect skin from the harmful effects of prolonged pressure. Time series results showed that normalized peak perfusion response was significantly lower with cooling. Time-dependent spectral analysis results suggested that both metabolic and myogenic responses contribute to this protective effect [129, 130].

![Graph](image.png)

**Figure 4.4: Theoretical graph of pressure plotted against time with temperature as a further variable, reproduced from Lachenbruch [94].**

Previous findings were strengthened by similar heated indentation tests on swine conducted by Laizzo [131], in which a pressure was applied for 5 hours at three different temperatures of 35, 40 and 45°C. Pressure ulcers developed in all three samples; increasing temperature resulted in more progressed ulceration. The same
test was then conducted at 25°C. At this temperature there was no sign of pressure ulcer occurrence upon test completion.

Local subcutaneous tissue oxygenation is also closely linked to muscle tissue viability and is directly influenced by the internal pressure emanating from the IT which causes compression and vessel closure. Tissue oxygenation data can be used to measure the effectiveness of cooler skin temperatures in protecting against pressure ulcers. Data from literature [94, 132], suggests a 14% reduction in pressure can be equated to a 3°C reduction in skin temperature. It must be taken into account that this is a patient specific method which may not be wholly representative; however, these findings are in line with previous analyses of skin tissue perfusion data [94, 128-130]. It is therefore prudent to take a balanced approach during cushion selection as both material and skin temperature play an important role in both material performance and pressure ulcer formation.

4.3.4 Environmental effects - moisture and humidity

It is clinically recognised that there is a need to prevent moisture accumulation to effectively prevent skin breakdown [133]. Local perspiration (moisture) is prevalent at raised temperatures - this can lead to skin maceration and this increases susceptibility to skin breakdown. Friction can also be increased by the build-up of moisture [94]. Humidity can also have an effect on cushion properties. In-service thermal conditions were imitated by Stewart et al and it was found that the relative humidity increased by 10.4% at the skin cushion interface after one hour of seating for an average healthy male subject. Viscoelastic materials tend to soften in humid conditions [97, 124]; however, the use of a liquid proof seating cover often alleviates the need to take the effect of humidity into account. For this
reason, the effects of humidity on material properties are disregarded in this research.

4.4 Pressure mapping as a method for detecting pressure ulcer formation

The FSA pressure mapping system, manufactured by Vista medical (Figure 4.5), is a clinical tool used to assess pressure distribution at an interface, such as that between a person’s buttock and thigh area and a seating surface. The tool comprises a pressure sensing mat (430mm x 430mm) containing 256 sensors that is connected through an interface module to a computer. The contour map displays visually the pressure distribution at the interface. It is widely used in wheelchair seating prescription. However peak pressure values and average pressure values have been shown to possess low test repeatability and display volatility between different cushion samples [134-136]. In many cases pressure measurements can be scattered by the presence of creep, drift and hysteresis which can influence pressure readings [135]. Prior to taking a reading, the user is placed on a hard flat surface. Once the user is sitting centrally and symmetrically on the pressure mat, the recommended wait periods prior to recording of pressure measurements are between 6 and 10 minutes, this is to allow both material and sensor creep to dissipate [136, 137].
As previously discussed, peak pressures occur in the muscle tissue located near the bony prominences in the trunk region and this is where DTI ulcers radiate from. Pressure values in these areas are often several times larger than pressure values at the interface. The effectiveness of an interface pressure map reading has therefore been questioned [3]. However pressure mapping is generally used as an indicator of high pressure regions at the cushion-trunk interface, which can in turn be used as guidance in the preparation of a personalised cushion for wheelchair users with often non-symmetric trunk regions.

Brienza et al [123] in a study of elderly ‘at risk’ wheelchair users found that there was a significant link between the development of pressure ulcers, the highest interfacial pressure and the average of the four highest interfacial pressures. This study also found that the location of pressure ulcers coincided with the location of peak interface pressure in a generic parallelepiped shaped polyurethane foam sample. Personalised cushions were also tested in this clinical trial. These cushions were clinically individualised to try and increase surface interface area and reduce peak loads at the IT bones. Pressure ulcer formation for personalised cushions was found to be linked to high shear forces. This work was backed up by
the findings of Conine et al [138]. In a wide ranging review, Reenalda et al [139] agreed that there seemed to be a qualitative relation between interfacial pressure and pressure ulcer development. However, studies displayed large heterogeneity, resulting from different kinds of bias. Sources of bias included heterogeneity in subjects and size of the subject population; lack of standardization of the research protocol; lack of standardization of the definition and measurement of the prognostic and predictive factors (interfacial pressure) and lack of standardization of the definition of outcome measurement (pressure ulcer development). The lack of a clear relation between pressure ulcer formation and interfacial pressure combined with several individual intrinsic development factors such as level of mobility, body composition, nutritional state and wound history, mean that no algorithm has thus been formulated to link the two pressure measures [107, 139, 140]. As already discussed, FEA of the human buttocks region have shown the differences in peak pressure around the bony prominences to be much higher than the peak interface pressure. Interfacial pressure does however have a correlation to peak pressure, providing guidelines as to where pressure ulcers often develop.

4.5 Summary

The usage of polyurethane foam in seating was discussed in this chapter. Pressure ulcers, which are a common occurrence in wheelchair seating, were discussed in detail. The main extrinsic factors which cause pressure ulcers were considered. Interface pressure mapping was introduced in Chapter 8 to aid in the validation of material model parameters.

This chapter, along with Chapters 2 and 3, combined to provide an extensive overview of all pertinent literature. At this stage, material testing is introduced and
subsequently relevant results are analysed. These results are then used to both identify and validate material model parameters in Chapters 7 and 8 respectively. In Chapter 9, some of the extrinsic factors in ulcer formation are used as performance indicators of simulated wheelchair cushion solutions. Conclusions from the work are then presented in Chapter 10 along with suggestions for future work.
Chapter 5  Materials testing

Material testing was undertaken to characterise the behaviour of viscoelastic polyurethane foam materials. Characterisation was achieved firstly through the analysis of test results and secondly through the generation of material models capable of simulating foam in-service behaviour. Polyurethane foam is recognised as being a hyperelastic-viscoelastic material. Therefore, in order to accurately represent the structural behaviour of this material, both hyperelasticity and viscoelasticity needed to be modelled. All tests described in Sections 5.1 and 5.2 were conducted to characterise hyperelasticity and viscoelasticity respectively.

To generate reasonable hyperelastic material model parameters it is advisable to use data from tests which represent material in-service behaviour [2, 141-143]. Data from axial, biaxial, simple shear, planar and volumetric tests are commonly conducted to identify hyperelastic material parameters. In seating, axial compression and shear are the most dominant deformation mechanisms. The presence of body temperature also influences material performance [144]. Temperature dependent axial compression and simple shear material test procedures, (Section 5.1), were therefore conducted to generate hyperelastic material model parameters.

Tests were also conducted to improve understanding of the inherent viscoelastic behaviour of polyurethane foam. Appropriate tests, described in Section 5.2, were conducted to enable observation of stress relaxation and creep behaviour, which were the two most prominent viscoelastic phenomena. Separate sets of viscoelastic material model parameters were generated from the stress relaxation and creep tests procedures.
Validation of both the hyperelastic and viscoelastic material parameters was required. Two different indentation procedures, presented in Section 5.3, were conducted to validate each data set separately. Firstly, tests were conducted using a buttock-shaped Rigid Loading Indenter (RLI). The results of this complex loading indentation procedure were used to validate the hyperelastic material parameters. The second set of validation tests were conducted using an Indentation Force Deflection (IFD) indenter. Table A.1.1 presents a list of all the material model parameter identification and validation tests that were conducted.

Initially eight different materials were tested: two closed cell materials and six open celled materials. Two of the open-celled materials were conventional materials and four were purposely manufactured viscoelastic materials. Due to time constraints and the similarity of many of the initial material test results; it was decided to concentrate on testing three materials which were commonly used in wheelchair seating. The comprehensive programme of material tests, described above, was conducted on two viscoelastic polyurethane foams and one conventional polyurethane foam. Sunmate medium is a type of viscoelastic, open-celled polyurethane foam, manufactured by Dynamic Systems Incorporation (DSI). There are two types of medium density foam being manufactured by DSI, one is blue in colour and is not fire resistant and the other is grey in colour and is fire resistant. From this point onwards, the blue foam with a density of 80kg/m$^3$ will be referred to as Sunmate blue. Sunmate grey, which has a density of 90kg/m$^3$, is a fire resistant material - flame-resistant additives are mixed with the foam’s constituent elastomer during its manufacturing process. Sunmate grey is manufactured to have the same material properties as the Sunmate blue, as well as providing additional fire-resistive properties [145]. Kayfoam FS-40, referred to in
this work as Kayfoam, is manufactured by Kaymed. Kayfoam is a type of open-celled elastomeric foam with a density of 40kg/m³.

5.1 Parameter identification testing - hyperelastic model

Axial compression and simple shear material test procedures were conducted on Sunmate and Kayfoam samples. Test results were used to identify numerical material model parameters for an Ogden Hyperfoam material model. Simulations were then developed using FE software, in which, this material model was utilised to predict the behaviour of foam under complex loading conditions.

5.1.1 Static testing - axial compression

Room temperature axial compression testing

Axial compression tests were conducted on samples in accordance with ‘ISO 3386: Polymeric materials, cellular flexible – Determination of stress-strain characteristic in compression’ [146]. All axial testing was undertaken on samples with dimensions of 150mm ± 1mm length by 150mm ± 1mm width by 50mm ± 1mm height. The 150mm length by 150mm width dimensions were chosen as they were above the minimum 2:1 length/width to thickness ratio specified in the standard. All samples were conditioned at the testing temperature of 23 ± 2°C for 16 hours prior to testing. Each test-piece was inserted centrally between two horizontal platens attached to a Lloyds testing machine (LR30k), fitted with a 2.5kN load cell. The bottom platen was height adjustable at its four corners; this ensured that both platens could be maintained parallel to each other during testing. A 0.5N preload was applied to the test-piece and was retained when the specimen was considered fully decompressed. This ensured that the test-piece would not loosen on decompression during repeated test cycles. For the first test, the sample
was compressed to a strain of 0.7 at a strain rate of $143 \times 10^{-6}/s$ (corresponding to a crosshead feed rate of 1mm/min) and then decompressed at the same strain rate. This cycle was repeated immediately three times and on the last compression cycle, load-deflection data was recorded. The initial three cycles of the virgin foam sample were applied to minimise the effect of stress softening (Mullins effect) [147]. This test procedure was then repeated, without the initial three cycles, at strain rates (which were governed by the limitations of the testing machine) of $714 \times 10^{-6}/s$, $714 \times 10^{-5}/s$, $14.28 \times 10^{-3}/s$, $35.7 \times 10^{-3}/s$ and $71.4 \times 10^{-3}/s$. A list of crosshead speeds corresponding to each strain rate is provided in Appendix 1. Compression tests at these strain rates were conducted on each of the Sunmate blue, Sunmate grey and Kayfoam samples.

Figure 5.1: Axial compression testing set-up.

*Temperature controlled axial compression testing*

Test procedures described in the previous section were repeated at an elevated temperature of 30°C and tests were also conducted at 37°C (human body
temperature). All elevated temperature testing procedures were conducted using a strain rate of $35.7 \times 10^{-3}$/s. Raised temperature testing was conducted in a custom built and designed temperature controlled airflow system, the basic operation of which is described in the schematic Figure 5.2(a). Air which was heated by two 45W Pfaffenberg storage heaters in the external heating chamber was circulated around the system via two 0.5A axial fans. The compression testing took place in the testing chamber (Figure 5.2(b)). The external heating chamber had a volume of $0.125 \text{m}^3$ which was twenty-five times larger than that of the testing chamber. This large volume ratio helped achieve accurate temperature control in the smaller volume testing chamber, as air temperature was stabilised in the larger volume external heating chamber before being circulated around the system. National Instruments (NI) equipment was used to monitor and control the system’s temperatures during testing. The temperature in the system was measured and monitored using T-type thermocouples with temperature module NI-9211. The system was controlled by the digital output module NI-9472. Both modules were mounted in an NI cRIO-9073 DAQ system.
5.1.2 Static testing - simple shear

A dual-lap test rig, similar to that developed by Siriruk et al [148] for testing in simple shear, was designed and constructed. Shear testing was conducted in accordance with ‘ISO 1827: Rubber, vulcanized or thermoplastic - Determination of modulus in shear’ [149]. Simple shear test samples had the following dimensions: 6mm thickness, 20mm width and 25 mm length. Test-piece size was consistent throughout, as shear modulus is strongly dimension sensitive [150]. Samples were bonded on both sides to rigid plates prior to testing using cyanoacrylate adhesive. Tests were conducted at a shear strain rate of $26.7 \times 10^{-4}/s$ until shear failure. Care was taken to ensure samples failed due to shear and not adhesive failure: upon test completion, failed samples were examined to ensure the adhesive was intact. If there was any uncertainty about the validity of test results the test was disregarded and repeated.
5.2 Parameter identification testing - viscoelastic model

Stress relaxation and creep tests were conducted on Kayfoam and Sunmate blue foam samples. Stress relaxation and creep test results were used to identify two separate sets of parameters for generalised Maxwell-Wiechert and generalised Kelvin-Voigt models respectively. These models were used to simulate viscoelastic phenomena and in conjunction with the hyperfoam model identified in Section 5.1, can be used to simulate complex foam deformation behaviour.

5.2.1 Time dependent testing - stress relaxation

The standard ‘ISO 3384 - Rubber, vulcanized or thermoplastic -- Determination of stress relaxation in compression - Part 1: Testing at constant temperature’ [151] was used as a guideline for conducting constant displacement stress relaxation testing. This test procedure measured the decrease in counterforce exerted by a foam test-piece which was compressed to a given strain and maintained at that
strain at a test temperature of 23 ± 2°C. All stress relaxation testing was undertaken on samples with dimensions of 150mm ± 1mm length by 150mm ± 1mm width by 50mm ± 1mm height. All tests were conducted on Kayfoam using the Lloyds LR30k material testing machine. Prior to testing all samples were mechanically conditioned as described in section 5.1.1. Samples were strained to 0.7 at the maximum machine strain rate of 0.0714/s and held for an 8 hour time period. This time was chosen to replicate the daily occupancy of a wheelchair user. The dissipating force was monitored over the entire period of the test.

5.2.2 Time dependent testing - creep

Creep testing apparatus

Creep compression testing was conducted on Sunmate blue foam. The standard ‘ISO 10066 Flexible cellular polymeric materials – Determination of creep in compression’ was used to carry out all creep compression testing [152]. The Lloyds LR30k machine, previously mentioned in Sections 5.1.1 and 5.2.1, was capable of conducting the required experiments. However, the longevity of the creep testing necessitated the use of a separate apparatus. A customised test apparatus was therefore designed and constructed and this is shown in Figure 5.4.
The creep testing apparatus was constructed entirely from mild steel. Identical and equidistant holes were drilled at the corners of the top two flat plates. These plates were then bolted together and separated by 30mm spacers. Four vertical bars were bolted to the four corners of the base plate. Additional weights were placed evenly onto the top plate to provide a suitable initial strain value of 0.5, which was typical of a seating application. The top plates slid freely up and down the four vertical bars shown in the figure, compressing the sample beneath.

The creep compression test apparatus was placed into a temperature chamber to enable temperature controlled creep testing, see Figure 5.6. Two angled brackets were attached to the top plate; the bottom of a Variohm linear potentiometer was connected to these brackets via a small cylindrical bar. All displacement readings were taken using this potentiometer which had a stroke of 100 mm, an accurate reading range of ± 0.01mm and a maximum operating temperature of 175°C. The potentiometer was powered by a 5V power supply. All voltage readings in the range of 0 – 5V were linearly proportional to a mechanical stroke in the range 0 –
100 mm. Potentiometer readings were recorded with a digital input NI 9205 module connected to a NI cRIO-9073 DAQ system.

_Creep test apparatus calibration_

Creep tests, in which identical loads were applied at identical test temperatures of 23 ± 2°C, were conducted using the creep testing apparatus and the Lloyds LR30k material testing machine with a calibrated 2.5kN load cell. For calibration purposes, the results of the comparative tests are presented in Figure 5.5. The discrepancy noted at the beginning of the tests can be attributed to different loading rates. The test conducted in the custom built test rig had a slightly higher strain when compared with the Lloyds test rig creep test. This resulted in minor differences in test results at the very early stages (<50 seconds), which had little effect on long-term viscoelastic material behaviour. This corresponded to previous findings on polymer materials [33]. The two curves matched each other over the remainder of the 20 minute test, beyond the initial loading period. After 100 seconds a difference in creep of 1.6% was noted. This reduced to 0.5% after 200 seconds. The similarities between the curves demonstrated that the creep test apparatus was capable of providing results to the required accuracy.
Figure 5.5: Comparison of creep tests conducted on custom-built creep test set-up and the Lloyds testing machine.

Creep testing procedures

Identical samples, 50mm ± 1mm in height and 125mm ± 1mm length by 125mm ± 1mm width, were prepared from the same batch of material. Prior to testing, each sample was mechanically conditioned as described in Section 5.1.1. Each mechanically conditioned sample was then positioned centrally upon the base plate of the creep compression apparatus. The compression plate was held at the sample height from the bottom plate using two identical spacers. The combined weight of the compression plates and the additional weights was 20.6kg. The apparatus, with sample in place, was placed in the test chamber and thermally conditioned in the unstrained state at the test temperature for twelve hours prior to testing. This time was used to ensure that the complete system was at uniform temperature. To begin a test, the spacers were removed and the weight was released onto the test sample.
**Temperature controlled creep testing apparatus**

Six additional creep tests were conducted at elevated temperatures to enable more accurate representation of in-service material behaviour. To facilitate this, the creep testing apparatus was placed inside a temperature chamber as depicted in Figure 5.6. The top of the potentiometer was connected to an external steel bar, which was in turn held vertically in place using two aluminium alloy blocks. An opening in the top of the temperature oven allowed the bottom of the potentiometer to feed through into the oven and be connected to the top plate. All thermal measurements were taken from within the centre of the sample, measured with a T-type thermocouple and recorded using an NI-9211 module connected to a NI cRIO-9073 DAQ system. Temperature measurements were recorded throughout the period of each test. Tests were conducted at six predetermined temperatures: 30, 45, 60, 70, 83 and 115°C. The tests at the highest and lowest temperatures were used for prediction validation purposes; the results of the other tests were used to generate these predictions. All tests were conducted over a 20 hour period, except the 30°C validation test which lasted 35 days. The response during the loading period of test procedures was ignored and only the data obtained during the period of constant displacement or constant load were used to determine material properties [153, 154].
5.3 Model validation testing

Rigid Loading Indentation (RLI) testing was conducted on the polyurethane foam samples; the hyperelastic material model was validated using this procedure. The RLI indenter had a realistic shape as it was the standard buttock shaped indenter [155]. Indentation Force Deflection (IFD) testing was then conducted to validate the viscoelastic portion of the material model. IFD testing was conducted over extended time periods; this was a more appropriate validation procedure for a model to be used in long-term seating simulations. The procedures involved in the validation of thermal conductivity constants are also explained in this section.

5.3.1 RLI testing

A buttock-shaped wooden rigid loading indenter based on ‘ISO: 16840-2, Wheelchair seating –Part 2: Determination of physical and mechanical characteristics of devices intended to manage tissue integrity – Seat cushions’
was ingressed into a foam sample. The foam sample had rectangular dimensions of a typical wheelchair cushion with a length of 500mm, width of 380mm and height of 75mm. Indentation testing was conducted at room temperature 23°C ± 2°C. During the testing, commercially available FSA seating interface pressure mats (Vista Medical) were placed at the cushion-indenter interface and at the cushion-base interface. Each pressure mat had 256 pressure sensors spread evenly across its 430mm by 430mm area. These pressure mats monitored the contact pressure during each loading step.

The indenter, weighing 6.1kg, was placed onto the top pressure mat and loaded evenly with 49.7kg to give a combined weight of 55.8kg. Ten pressure readings, from each pressure mat, were recorded at this weight over a period of 10 seconds. A further 25.4kg was added to the indenter to give a combined weight of 81.2kg and ten readings were recorded at this point. Finally an additional 25.1kg was added to give a total weight of 106.3kg and ten readings were also recorded at this weight. A further set of readings were recorded after a fifteen minute settling period. The fully loaded indenter is shown in Figure 5.7.
IFD testing

IFD tests were conducted on the foam samples. A circular indenter was machined to generally conform with ‘ISO 2439: Flexible Cellular Polymeric Materials-Determination of Hardness (Indentation Technique)’ [156] but scaled down to 81.2 mm in diameter, to be compatible with the 150mm square test-pieces. The indenter was axially indented into the foam samples at strain rates of $71.4 \times 10^{-5}/s$ and $71.4 \times 10^{-4}/s$ to a strain of 0.7 using the Lloyds testing machine. After each loading, the indenter was held at a 0.7 strain for a period of 8 hours.
5.4 Summary

Static and time-dependent material testing was conducted on three foam types. A table listing all test procedures and which materials they were conducted on is presented in Appendix 1. In the following chapters the results recorded from these tests are appropriately formatted and curve-fitted to the chosen material models for hyperelasticity and viscoelasticity. Material constants are then extracted and used to model material behaviour in FE simulations.

Further indentation tests were conducted to analyse the accuracy of the simulated behaviour reported using these material constants. The RLI (Rigid Loading Indenter) test was conducted as a validation procedure to ensure that the values calculated using the Hyperfoam model, were suitable to simulate a complex indentation load case. IFD (Indentation Force Deflection) testing was also conducted to enable validation of the viscoelastic part of the model over realistic time periods for seating applications. The results of these tests were compared with results from FE simulations in Chapter 8.
Chapter 6  Results of materials testing

Results from the parameter identification test procedures, described in Sections 5.1 and 5.2 were compared and analysed for characterisation purposes here. The results are presented in two separate sections; static test procedures and time dependent test procedures.

6.1 Static test procedures

6.1.1 Temperature dependence
Load-unload compression testing was undertaken on Sunmate blue viscoelastic polyurethane foam, Sunmate grey fire resistant viscoelastic polyurethane foam and Kayfoam conventional polyurethane foam at three different temperatures, following the procedures outlined in Section 5.1.1. Test results displayed in Figures 6.1(a), 6.1(b), 6.2(a) and 6.2(b) verifies that, as expected, both the viscoelastic Sunmate material properties display temperature dependency. The decreasing peak stress values were indicative of gradual material softening as test temperatures increase. Very similar reductions in peak stress of 14.2% and 14.3% were noted between tests at 20°C and 37°C for the blue and grey materials respectively, meaning that each material had a similar dependency on temperature. It was noted that all materials displayed similar moduli at lower strain values, regardless of testing temperature. The relatively high strain rate of \(35.7 \times 10^{-3}/s\) for the testing procedure meant that the effect of temperature on material performance was diminished at lower strains. This higher strain rate of testing, which was required for material modelling purposes, was therefore a more dominant factor than temperature dependence. However, both products did soften
proportionally over the temperature range of the tests. This thermal dependence was to be expected as both materials are manufactured to provide enhanced viscoelastic performance when compared with conventional polymeric foam materials [39]. The Kayfoam conventional material was not influenced by temperature to the same levels as the Sunmate materials, see Figure 6.3. A reduction in peak compressive stress of 5.5% was recorded between 20°C and 37°C. A greater reduction of 6.5% was recorded between 20°C and 30°C. The similar modulus recorded at both elevated temperatures suggested that the material displayed quite a low dependence on temperature. Sample temperatures during test loading were recorded and are displayed in Appendix 2. Experimental test results were quoted in Table 1.

![Stress-strain graph](image)

**Figure 6.1(a): Test results displaying temperature dependence of stress-strain relation in Sunmate blue material. Box represents area to be magnified in Figure 6.1(b).**
Figure 6.1(b): Magnified box from Figure 6.1(a).

Figure 6.2(a): Test results displaying temperature dependence of stress-strain relation in Sunmate grey material. Box represents area to be magnified in Figure 6.2(b).
Figure 6.2(b): Magnified box from Figure 6.1(a).

Figure 6.3: Test results displaying lower temperature dependency of stress-strain relation in Kayfoam material.
### Table 6.1: Peak stresses at three temperatures for a strain of 0.8.

<table>
<thead>
<tr>
<th>Material</th>
<th>Stress(_{20^\circ\text{C}}) (Pa)</th>
<th>Stress(_{30^\circ\text{C}}) (Pa)</th>
<th>Stress(_{37^\circ\text{C}}) (Pa)</th>
<th>Reduction 20°C-37°C (%)</th>
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</thead>
<tbody>
<tr>
<td>Sunmate blue</td>
<td>37709</td>
<td>35309</td>
<td>32350</td>
<td>14.2</td>
</tr>
<tr>
<td>Sunmate grey</td>
<td>62044</td>
<td>57844</td>
<td>53160</td>
<td>14.3</td>
</tr>
<tr>
<td>Kayfoam</td>
<td>30155</td>
<td>28200</td>
<td>28500</td>
<td>6.5</td>
</tr>
</tbody>
</table>

#### 6.1.2 Rate dependence

Load-unload compression testing was undertaken on Sunmate blue viscoelastic polyurethane foam, Sunmate grey fire resistant viscoelastic polyurethane foam and Kayfoam conventional polyurethane foam at six different strain rates, following the procedures outlined in Section 5.1.1. The results from the varying strain rate compression tests on Sunmate blue foam are displayed graphically in Figure 6.4. Sunmate blue foam exhibits differences in stress resulting from varying strain rates. A reduction in peak stress of 43.5% was noted between the highest strain rate of \(71.4 \times 10^{-3}/\text{s}\) and the lowest strain rate of \(143 \times 10^{-6}/\text{s}\) (Figure 6.4). For the same decrease in strain rate, the peak stress of the Sunmate grey sample (Figure 6.5) reduced by 39.7%. Both the Sunmate foams showed a considerable dependence on strain rate, with similar percentage reductions in modulus experienced by both samples over the range of strain rates. The same test was conducted on Kayfoam (Figure 6.6), with a reduction of 24.3% in peak stress occurring between the highest and lowest strain rates (Table 6.2). The lower percentage reduction in stress exhibited by the Kayfoam conventional material compared with the viscoelastic materials was indicative of the heightened viscoelastic properties of the Sunmate materials. The lower strain rate dependence of the conventional foam was expected as strain rate dependency is a viscoelastic phenomenon [38, 39]. For all materials, higher levels of instantaneous stress softening of the constituent material occurred at lower strain rates; this was
depicted by the shallower load curves at lower strain rates. At higher strains, the constituent material had less time to soften and this led to steeper load curves.

### 6.1.3 Higher strain performance

Both the Kayfoam conventional foam and the Sunmate blue viscoelastic foam samples displayed similar peak stresses at lower test strains. However, at the maximum strain of 0.8, the Sunmate grey sample reached a much higher peak stress value, some 57% higher than the Sunmate blue sample. This trend repeated itself at all strain rates and temperatures. It is suggested that this higher stress value arose from the inclusion of fire retardant particles in the physical composition of the foam [157].

![Figure 6.4: Axial compression stress-strain load curves for Sunmate blue material loaded at six different strain rates (s⁻¹).](image-url)
Figure 6.5: Axial stress-strain load curves for Sunmate grey material loaded at six different strain rates (s\(^{-1}\)).

Figure 6.6: Axial stress-strain load curves for Kayfoam material loaded at six different strain rates (s\(^{-1}\)).
Table 6.2: Peak stresses at different strain rates for a strain of 0.8.

<table>
<thead>
<tr>
<th>Material</th>
<th>Stress $143 \times 10^{-6}$ (Pa)</th>
<th>Stress $71.4 \times 10^{-3}$ (Pa)</th>
<th>Reduction (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Sunmate blue</td>
<td>25530</td>
<td>45152</td>
<td>43.5</td>
</tr>
<tr>
<td>Sunmate grey</td>
<td>43794</td>
<td>72671</td>
<td>39.7</td>
</tr>
<tr>
<td>Kayfoam</td>
<td>27423</td>
<td>36234</td>
<td>24.3</td>
</tr>
</tbody>
</table>

6.1.4 Shear performance

Shear testing results from tests conducted in accordance with Section 5.1.2 are presented in Figure 6.7. Sunmate grey and blue viscoelastic materials display clear differences under shear loading. The slight downturn noted at the end of the loading curve for the Sunmate grey sample represents the beginning of sample failure. The results may have also indicated that the particles involved in providing fire resistance for the Sunmate grey material contribute to a decrease in the shear modulus of the material. The relatively low shear modulus of the Sunmate grey sample was in sharp contrast to its relatively high compressive modulus. The Kayfoam material, which had a much lower density, displayed similar shear behaviour to the Sunmate grey sample.
Figure 6.7: Simple shear stress-strain load curves obtained using device shown in Figure 5.3.

6.2 Time dependent test procedures

6.2.1 Stress relaxation

Stress relaxation testing was conducted over an eight hour time period according to the guidelines described in Section 5.2.1. Results of the stress relaxation procedures conducted on the three polymeric foam materials are displayed graphically in Figure 6.8. When displayed in linear time format, the loading plots exhibit a steep decline signifying sharp initial instantaneous relaxation and this made comparison of results difficult. The behaviour of the materials over the extent of the test was more easily shown with time represented on a $\log_{10}$ time scale. Stress values were plotted in normalised format as this allowed for easy comparison of stress relaxation behaviour between samples. Normalised viscoelastic data was also required as input for modelling viscoelastic material.
behaviour. It was noticed that all materials relaxed rapidly immediately after the instantaneous compression. The two viscoelastic materials showed higher rates of relaxation in this initial stage. After 1,000 seconds, 89.1% and 89.2% of the total relaxation (total relaxation period was 28,800 seconds) had taken place for the Sunmate blue and Sunmate grey foams respectively. The conventional Kayfoam material relaxed to 77.8% after this 1,000 second period. Overall, the three materials displayed relatively similar stress relaxation behaviour.

![Log Time (sec) vs Normalised Stress (Pa) for Sunmate blue, Sunmate grey, and Kayfoam](image)

**Figure 6.8: Normalised stress relaxation for 8 hour hold.**

### 6.2.2 Creep

Creep testing was carried out according to the test methodology explained in Section 5.2.2. The results of selected tests at 45°C, 60°C, 70°C and 83°C were plotted in normalised creep strain format in Figure 6.9. It was assumed here that the loading period takes 100 seconds to complete and so creep was therefore measured from 100 seconds into the test [153, 154]. Normalised creep strain, \( \varepsilon_n(t) \), is defined in Equation 6.1:
\[ \varepsilon_n(t) = \frac{\varepsilon(t)}{\varepsilon_{100}} \]  

where \( \varepsilon(t) \) is the creep strain and \( \varepsilon_{100} \) is the creep strain after 100 seconds.

Figure 6.9: Normalised creep compression curves plotted over 20 hour test periods at four different temperatures.

Creep tests were conducted on samples of Sunmate blue over 20 hour periods. Test data was used to create long-term predictions of creep behaviour using a superposition principle. This required physical testing at elevated temperatures; therefore tests were conducted over a range of temperatures which allowed these long-term predictions to be made. It was noted in Figure 6.9 that the creep compression curves at the higher temperatures, 70°C and 83°C, display a significant acceleration in creep strain over the period of the test. The normalised creep strain measured after the duration of the test was 3.7 times higher for the
83°C test than the 45°C test. Increased temperatures are known to cause hydrogen bond disruption which increases the amount of chain slippage. This chemical behaviour was the primary reason for the accelerated creep rate at these higher temperatures [158]. This acceleration in viscoelastic effects around 70°C has previously been noted by Ronan et al who conducted dynamic and static testing procedures on a range of different polymeric materials and Cui et al who investigated stress relaxation behaviour in silicone materials [87, 159].

6.3 Summary
The primary reason for conducting these test procedures was to provide suitable data sets for appropriate material models. The secondary reason for conducting these tests was to use the results to analyse the relevant behaviour of a set of foam materials under a range of load cases appropriate to their intended use.

Results show that the Sunmate grey material exhibited higher stiffness at higher strain values than the Sunmate blue and Kayfoam materials. This higher stress value arose from the inclusion of fire retardant particles in the physical composition of the foam. Both Sunmate materials displayed advantageous higher temperature properties. Overall, the results suggest that the Sunmate blue material exhibited superior properties and performance than the other two materials tested and analysed. It was decided to concentrate on Sunmate blue and Kayfoam materials for further investigations, as based on test results thus far, the Sunmate blue exhibited more desirable properties than Sunmate grey.
Chapter 7  Material model parameter identification

Parameter identification is defined as the process of estimating the values of the parameters that govern a system from data derived from the observed behaviour of the system [160]. The modelling of the structural behaviour of elastomeric materials is, as previously discussed, a long established focus of materials science that often requires the use of complex mathematical strain energy functions. The complexity involved stems from the material’s inherent non-linear behaviour, which is also what makes it a vital component material in many engineering applications.

The randomised cellular structure of foamed material sets it apart from rubber; this structure means that foam is compressible under an applied force, whereas rubber is commonly assumed to be almost incompressible when first loaded. The difficulty in modelling foam materials therefore arises from the fact that it has a randomised cellular structure, with struts that consist of a non-linear parent material.

In this chapter, compressible hyperelastic and viscoelastic material model parameters will be identified using respectively Ogden’s compressible strain energy Hyperfoam function and Prony Series representations of appropriate theoretical spring-dashpot systems. The details involved in creating robust and accurate Hyperfoam material parameters will be discussed. Two sets of time dependent viscoelastic constants will be generated using two different long-term test procedures. Time-Temperature Superposition (TTS) theory will also be utilised with time dependent creep test data to create long-term predictions for material behaviour in creep. Predictions of extreme value (high temperature/long
time) material behaviour will then be made using the Williams Landel and Ferry (WLF) theory [49]. Predictions of material creep based on the chemical reaction rate are also made using the Arrhenius model. Finally, a cellular material thermal conductivity model will be utilised to develop thermal conductivity constants. A fully time and temperature-dependent description of material behaviour is provided by the development of this range of material parameters.

7.1 Ogden Hyperfoam model

7.1.1 Axial

Results from static axial compression test procedures, presented in Section 6.1, were used to calculate constants for the 2nd order form of Ogden’s Hyperfoam material model [48]. The material’s transverse stretches, $\lambda_1$ and $\lambda_2$, were negligible during the axial compression test. This suggested, as previously discussed, that an assumption of Poisson’s ratio $\nu = 0$ was valid. This assumption allowed the simplification of Ogden’s Hyperfoam model to permit calculation of the nominal stress, $T_2$, in the $\lambda_2$ loading direction, (repeating Equation 3.18).

$$T_2 = \frac{\partial U}{\partial \lambda_2} = \frac{2}{\lambda_2} \sum_{i=1}^{N} \frac{\mu_i}{\alpha_i} (\lambda_2^{\alpha_i} - J_{el}^{-\alpha_i})$$

(7.1)

7.1.2 Simple shear

Simple shear test data, presented in Section 6.1, was curve-fitted to the simple shear mode version of the material model presented in Equation 7.2. The simple
shear data was also separately used to simultaneously calculate $\mu_i$ and $\alpha_i$ coefficients along with axial data.

\[
T_s = \frac{\partial U}{\partial \gamma} = \sum_{j=1}^{2} \left[ \frac{2\gamma}{2(\lambda_j^2 - 1) - \gamma^2} \sum_{i=1}^{N} \frac{\mu_i}{\alpha_i} (\lambda_j^{\alpha_i} - 1) \right]
\]  

(7.2)

where $\gamma$ is the shear strain, $\lambda_1$ and $\lambda_2$ are the two principal stretches in the plane of shearing and are related to shear strain by:

\[
\lambda_{1,2} = \sqrt{1 + \frac{\gamma^2}{2} \pm \gamma} \sqrt{1 + \frac{\gamma^2}{4}}
\]

(7.3)

7.1.3 Curve fitting procedures

Least squares optimisation was used to determine the $\mu_i$ and $\alpha_i$ parameters for three load cases; the axial data set alone, the shear data set alone and finally using both data sets simultaneously. This highlighted the benefits of using more than one mode of test data when identifying material model parameters. The derivation of model constants followed best practise guidelines [2]. The axial model alone provided accurate results when replicating an axial compression procedure (Figure 7.1 – Experimental Data compared well with Model (Axial)). However the
parameters are completely unstable when attempting to model shear (Figure 7.1 – Experimental Data compared poorly with Model (Shear)).

Figure 7.1: Axial compression data for sample compressed to a strain of 0.78 at a strain rate of 71.4x10^{-4}/s at 23°C compared with an Ogden Hyperfoam model curve-fit for the axial, combination and shear cases.

Figure 7.2 depicts the different models’ accuracies when modelling a shear deformation. In this procedure, the model identified with shear data alone provided the most precise fit (Figure 7.2 – Experimental Data compared well with Model (Shear)) and the model fitted with axial data provided the least accurate fit as would be expected (Figure 7.2 – Experimental Data compared poorly with Model (Axial)).
Figure 7.2: Simple shear data for sample sheared to a strain of 1 at a shear strain rate of $26.7 \times 10^{-4}/s$ at 23°C compared with an Ogden Hyperfoam model curve-fit for the shear, combination and axial cases.

Overall the model identified with both datasets (Model (Axial + Shear)) fits accurately to the experimental data in Figures 7.1 and 7.2. These constants were derived from test modes which were most relevant to the material’s in-service deformation mode. The inaccurate curve fit derived using either axial or simple shear test data alone, demonstrates the importance of using more than one mode of deformation when attempting to simulate a complex deformation pattern. Some slight error is noticeable in the initial elastic region of the model (<10% of overall strain) in Figure 7.1, where the test data suggests a stiffer material than the model predicts. However material behaviour is satisfactorily modelled at higher strain values. This was deemed more important, as in-service deformation was generally larger than 10%. The overall accuracy of the model was therefore satisfactory as error at lower values of strain was deemed to have little significance for realising
the objectives of the research. The material parameters for each of the three models are displayed in Table 7.1.

**Parameter Results**

All material parameters identified from the curve-fits of Figures 7.1 and 7.2 were generated from test data extracted from material tests conducted on Kayfoam material at 20°C and the sets of parameters for this case are presented in Table 7.1. These procedures were also conducted on Sunmate blue samples at room temperature (Table 7.2) and both Kayfoam and Sunmate blue samples at 30°C and 37°C, see Table 7.3.

<table>
<thead>
<tr>
<th>N</th>
<th>( \mu ) (Pa)</th>
<th>( \alpha )</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>1.2E+04</td>
<td>5.3E+00</td>
</tr>
<tr>
<td>2</td>
<td>7.1E-03</td>
<td>6.5E+00</td>
</tr>
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</table>

**Shear Model**

<table>
<thead>
<tr>
<th>N</th>
<th>( \mu ) (Pa)</th>
<th>( \alpha )</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
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<td>2.72E+01</td>
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<tr>
<td>2</td>
<td>6.35E+00</td>
<td>-4.19E+00</td>
</tr>
<tr>
<td>1</td>
<td>1.18E+04</td>
<td>6.12E+00</td>
</tr>
<tr>
<td>2</td>
<td>5.61E-02</td>
<td>-7.39E+00</td>
</tr>
</tbody>
</table>

**Axial Model**

<table>
<thead>
<tr>
<th>N</th>
<th>( \mu ) (Pa)</th>
<th>( \alpha )</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>4.09E+04</td>
<td>2.89E+00</td>
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<td>2</td>
<td>7.10E-03</td>
<td>6.50E+00</td>
</tr>
<tr>
<td>1</td>
<td>4.64E+04</td>
<td>2.01E+01</td>
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<td>2</td>
<td>1.34E+02</td>
<td>-1.91E+00</td>
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<tr>
<td>1</td>
<td>3.05E+04</td>
<td>2.90E+00</td>
</tr>
<tr>
<td>2</td>
<td>-2.17E+04</td>
<td>2.93E+00</td>
</tr>
</tbody>
</table>

**Axial + Shear**

Table 7.1: Coefficients of Hyperfoam model for the Kayfoam material for three model cases.

Table 7.2: Coefficients of Hyperfoam model for the Sunmate blue material for three model cases.
Table 7.3: Coefficients (Axial + Shear) of Hyperfoam models for Kayfoam and Sunmate blue materials for tests at higher temperatures.

7.2 Viscoelastic model

Stress relaxation experiments were conducted on the Kayfoam material at room temperature. Creep experiments were conducted over a range of temperatures on the more temperature dependent Sunmate blue material. The results of these time-dependent tests, presented in Section 6.2, are used to characterise viscoelastic behaviour through the identification of appropriate material parameters.

7.2.1 Stress relaxation

The tensile relaxation (Young’s) modulus, $E(t)$, was measured during the axial stress relaxation procedure. The Prony series, pioneered by Gaspard de Prony in 1795, is a technique commonly used for representing the time dependent stress-strain behaviour in a viscoelastic material using a series of exponential terms. The Maxwell-Wiechert system, described in Equation 3.37, can be represented mathematically using a Prony series (Equation 7.4):
\[ E(t) = k_0 + \sum_{i=1}^{N} k_i e^{-t/\lambda_i} \]  

(7.4)

where \( \lambda_i = \mu_i / k_i \) is the relaxation time for the \( i^{th} \) Maxwell element and \( k_i \) is the relaxation magnitude. The long-term response is represented by \( k_0 \) and \( N \) is the order of the Prony series.

**Parameter Results**

A fourth order Prony Series was fitted to the normalised stress relaxation test data, with parameters identified using a least squares optimisation technique. An eighth order Prony series which provided a more accurate curve fit is displayed in Table 7.2. This series was then fitted to the test data. Both models were compared to normalised stress relaxation data (Figure 7.3). The eighth order model is capable of predicting stress relaxation, creep and strain rate variance of the polyurethane foam for a given strain and strain rate. The parameters for this model provide a very accurate response when compared with empirical data.
Figure 7.3: Normalised stress relaxation test data for Kayfoam sample compared with fourth and eighth order mathematical Prony series representations.

Table 7.4: Prony series parameters for a model fitted to stress relaxation data.

<table>
<thead>
<tr>
<th>$N$</th>
<th>$k_i$</th>
<th>$\lambda_i$ (s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>6.17E-04</td>
<td>1.01E-03</td>
</tr>
<tr>
<td>2</td>
<td>-1.27E-03</td>
<td>1.89E-03</td>
</tr>
<tr>
<td>3</td>
<td>8.99E-02</td>
<td>2.93E-01</td>
</tr>
<tr>
<td>4</td>
<td>1.15E-01</td>
<td>4.74E+00</td>
</tr>
<tr>
<td>5</td>
<td>8.03E-02</td>
<td>5.52E+01</td>
</tr>
<tr>
<td>6</td>
<td>7.72E-02</td>
<td>6.30E+02</td>
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<tr>
<td>7</td>
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</tr>
<tr>
<td>8</td>
<td>-3.01E-02</td>
<td>1.74E+08</td>
</tr>
</tbody>
</table>

7.2.2 Creep

The Sunmate blue material required viscoelastic modelling to simulate the foam’s behaviour accurately. The mechanical analogue to represent this behaviour consisted of several Kelvin-Voigt elements combined in parallel with a spring (Equation 3.38).
Parameter Results

A least squares curve fit was applied to optimise the $\rho_i$ constants, displayed in Table 7.5. Third and fifth order versions of the model described in Equation 3.38 were fitted to the creep compliance master-curve. The third order model displayed some inaccuracy in the earlier time scale. An improvement in accuracy was obtained from using the higher order model. The fifth order parameters presented here are suitable for modelling viscoelastic material behaviour in the Abaqus FE package.

<table>
<thead>
<tr>
<th>$N$</th>
<th>$k_i$</th>
<th>$\mu_i(s)$</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>5.60E+01</td>
<td>3.20E+04</td>
</tr>
<tr>
<td>2</td>
<td>1.80E+01</td>
<td>3.70E+07</td>
</tr>
<tr>
<td>3</td>
<td>6.50E+08</td>
<td>6.20E+04</td>
</tr>
<tr>
<td>4</td>
<td>4.10E+01</td>
<td>2.50E+06</td>
</tr>
<tr>
<td>5</td>
<td>4.70E+00</td>
<td>5.50E+07</td>
</tr>
</tbody>
</table>

Table 7.5: List of material parameters for 5th order generalised Kelvin-Voigt model.
Figure 7.4: Normalised creep compliance prediction for Sunmate blue compared with third and fifth order mathematical Prony Series representations.

7.2.3 Viscoelastic model implementation

The Prony Series parameters were implemented in conjunction with the Ogden Hyperfoam model parameters to simulate Kayfoam or Sunmate blue material behaviour at 20°C, 30°C or 37°C. Equation 7.5 updates the $\mu_i$ Ogden parameters by multiplying the original parameter by the relaxation Prony series to include viscoelastic relaxation behaviour using the relaxation moduli, $k_i$ and $\lambda_i$:

$$\mu^{\mu}_i = \mu^0_i \left(1 - \sum_{i=1}^{N} k_i (1 - e^{-t/\lambda_i})\right)$$  \hspace{1cm} (7.5)
where $\mu_i^0$ is the original Hyperfoam material parameter and $\mu_i^R$ is the relaxed Hyperfoam material parameter. Similarly, multiplication of the original Hyperfoam material parameters by the creep Prony series generates creep Hyperfoam parameters.

### 7.3 TTS and WLF predictions

#### 7.3.1 TTS – master-curve generation

Time-Temperature Superposition (TTS), described in Section 3.3.1, was applied to the creep curves obtained from the tests at 45°C, 60°C, 70°C and 83°C (see Figure 6.9), to create a reference curve, which gave long-term material predictions at 45 °C. The time-scale of each of the four curves was multiplied by a temperature dependent shift factor, $a_T$, moving each curve horizontally along the time-scale towards the reference temperature curve. This extended curve, displayed in Figure 7.5, is referred to as the master-curve and it should represent this material’s actual creep behaviour at the reference temperature. The master-curve is constructed by using sections from each of the four input creep curves (see Figure 7.5) where each section is signified by a different symbol. The shift factors for the material at this reference temperature are displayed in Table 7.6. The use of TTS to generate a master-curve provided creep predictions up to 28.6 days, at the reference temperature, from test data generated in 3.2 days.
7.3.2 Application of WLF theory

The WLF equation, presented in 3.3.2, was curve fitted to the log $a_T$ values (Table 7.6) to enable predictions for the behaviour at temperatures beyond the scope of testing. Parameters $C_1$ and $C_2$ were calculated using a least squares optimisation procedure as 5.64 and -101.13 respectively. An accurate curve fit was obtained, (refer to Figure 7.6).

Table 7.6: Log shift factors generated from the creation of a master-curve at reference temperature of 45°C.

<table>
<thead>
<tr>
<th>Temperature (°C)</th>
<th>45</th>
<th>60</th>
<th>70</th>
<th>83</th>
</tr>
</thead>
<tbody>
<tr>
<td>$a_T$</td>
<td>1.00E+00</td>
<td>5.25E+00</td>
<td>1.35E+01</td>
<td>3.45E+01</td>
</tr>
<tr>
<td>$\log a_T$</td>
<td>0.00E+00</td>
<td>7.20E-01</td>
<td>1.13E+00</td>
<td>1.54E+00</td>
</tr>
</tbody>
</table>

Figure 7.5: Viscoelastic polyurethane foam creep master-curve.
Figure 7.6: Master-curve log shift factors compared with WLF curve-fit. The WLF curve is extrapolated to enable predictions of material behaviour at 30°C and 115°C.

**Extreme value prediction – Long-term prediction**

Extrapolation of the WLF equation to a value of 30°C, using the calculated constants, revealed a log $a_T$ value of 0.98 (Figure 7.6) and corresponding WLF shift factor, $\Delta T$, of 9.6. This shift factor was applied to the time-scale of the creep master-curve and the resulting shifted WLF master-curve gave an approximation for the creep strain behaviour at 30°C, (Figure 7.7). Use of WLF theory in conjunction with master-curve theory can provide predictions for material behaviour 275 days into the future from 3.2 days of testing, thus potentially reducing testing time-scales by a factor of 86.

**Extreme value prediction – High-temperature prediction**

The WLF prediction at a temperature of 115°C is also presented in Figure 7.7. A WLF shift factor of $4.9 \times 10^{-3}$ was generated from a log $a_T$ value of -2.31 from the WLF curve in Figure 7.6. This factor was then applied to the time-scale of the
creep master-curve, generating the WLF prediction for creep in compression at 115°C (Figure 7.7).

![Diagram showing WLF predictions](image)

**Figure 7.7: TTS master-curve with associated WLF predictions.**

### 7.4 Arrhenius predictions

#### 7.4.1 Arrhenius predictions

The Arrhenius model, described in Section 3.3.3, was also applied to the same viscoelastic material, (Figure 7.8). Creep threshold values of 1.1, 1.15 and 1.2 normalised creep strain were chosen. The dashed lines represent the Arrhenius extrapolation, which is the best-fit for the high temperature threshold values. By extrapolating this line, an estimation of the time to reach a specific threshold value at lower temperatures could be made. The Arrhenius model predicts a time of 3.5 days for the material to reach a normalised creep strain of 1.1 at 30°C. For creep strains of 1.15 and 1.2, the predictions at the same temperature are 9.1 days and
15.3 days respectively. Strain threshold predictions shorten in time as the temperature increases to the highest temperature prediction of 115°C.

![Graph showing Arrhenius predictions](image)

**Figure 7.8: Arrhenius predictions for 1.1, 1.15 and 1.2 normalised creep strain.**

Values of temperature plotted in K and in °C.

### 7.5 Thermal conductivity

In order to build a full understanding of thermal behaviour, material thermal conduction values were required. Polyurethane foam is an insulating material, meaning that measurement of thermal conductance was a difficult procedure. The flexibility of the material increased the difficulty of this measurement as thermal conductance was coupled with material displacement, so that thermal conductance was dependent on both temperature and applied pressure. Instead of conducting costly and time consuming testing, it was decided to utilise an existing theoretical conductivity model for insulating foam materials proposed by Sinovsky and Glicksman [50, 51, 93].
There is several physical material parameters required when using this model as described in Section 3.4. The average cell diameter, the fraction of solid in cell struts and the open cell percentage (fraction) were all measured by microscopic inspection of a relevant sample. Figures 7.9 and 7.10 are microscopic images of Sunmate blue and Kayfoam respectively. The cell extinction coefficient was suggested by Sinovsky [93]. Other variables, such as $K$ and $\delta$ were calculated using equations described in Section 3.8.

![Microscopic image of Sunmate blue sample](image)

*Figure 7.9(a): Microscopic image of Sunmate blue sample, blurred sections represent voids on the sample surface. Lengths shown represent cell sizes.*
Figure 7.9(b): Zoomed microscopic image of Sunmate blue sample, blurred areas from (a) are focussed on here. Lengths represent cell sizes.

Figure 7.10(a): Microscopic image of Kayfoam sample, blurred sections represent voids on the sample surface. Lengths represent cell sizes.
Figure 7.10(b): Zoomed microscopic image of Sunmate blue sample, blurred areas from (a) are focussed on here. Lengths represent cell sizes.

<table>
<thead>
<tr>
<th>Symbol (Unit)</th>
<th>Variable</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>$K_w$ (m$^{-1}$)</td>
<td>Extinction coefficient of cell wall material</td>
<td>6.00E+04</td>
</tr>
<tr>
<td>d (m)</td>
<td>Cell diameter</td>
<td>2.60E-04</td>
</tr>
<tr>
<td>$f_s$</td>
<td>Fraction of solid in strut</td>
<td>9.50E-01</td>
</tr>
<tr>
<td>%O.C.</td>
<td>Open cell fraction</td>
<td>9.50E-01</td>
</tr>
<tr>
<td>$\lambda$ (W.m$^{-1}$.K$^{-1}$)</td>
<td>Thermal conductivity of polymer</td>
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</tr>
<tr>
<td>$\rho_f$ (Kg.m$^{-3}$)</td>
<td>Density of PU foam</td>
<td>4.00E+01</td>
</tr>
<tr>
<td>$\rho_s$ (Kg.m$^{-3}$)</td>
<td>Density of PU polymer</td>
<td>1.20E+03</td>
</tr>
<tr>
<td>$\sigma$ (W.m$^2$.K$^{-4}$)</td>
<td>Boltzmann constant</td>
<td>5.70E-08</td>
</tr>
<tr>
<td>$k_{air}$ 20°C (W.m$^{-1}$.K$^{-1}$)</td>
<td>Thermal conductivity of air</td>
<td>2.57E-02</td>
</tr>
<tr>
<td>K (m$^{-1}$)</td>
<td>Extinction coefficient</td>
<td>2.53E+03</td>
</tr>
<tr>
<td>$\delta$</td>
<td>Volume fraction of voids or cell interiors</td>
<td>9.67E-01</td>
</tr>
</tbody>
</table>

Table 7.7: Table of values used to calculate the thermal conductance of Kayfoam material at 20°C.

The variables presented in Table 7.7 were used in conjunction with Equation 3.43 to calculate the thermal conductance of the Kayfoam and Sunmate blue materials. To calculate different values of thermal conductivity, foam density values were re-calculated to represent a particular pressure/strain. The calculated thermal
conductance values for 20°C, 30°C and 37°C at 6 different strain values are displayed in Figure 7.11. Thermal conductivity values for Sunmate blue are displayed in Appendix 3 along with a table of property values used in their generation.

![Figure 7.11: Kayfoam thermal conductance values calculated at three temperatures and nine strain values using Sinovsky and Glicksman’s model [50, 51, 93].](image)

7.6 Summary

In this chapter, material parameters describing the behaviour of foam are generated. Test data is used to generate Hyperfoam and viscoelastic material model parameters. Further test data is used to create long-term physical and chemical based predictions for material behaviour. Finally physical parameters are used in conjunction with a theoretical model to generate thermal conductivity behavioural constants. Combinations of these parameters are used in the chapters that follow to simulate time and temperature dependent material behaviour.
Chapter 8  Material model parameter validation

Before using any of the material parameters identified in Chapter 7 in any analysis, they require validation through physical testing. Different physical validation testing procedures were undertaken to ensure each aspect of the model performed adequately. Indentation procedures were conducted to separately validate the Hyperfoam and viscoelastic models. Long-term and high temperature creep tests were separately conducted to validate the extreme value TTS-WLF and Arrhenius predictions. Finally, samples were heated in a controlled and measurable environment to validate the thermal conductivity coefficients.

8.1  Hyperfoam material model

8.1.1  FE simulation

The Abaqus FE package [2] was used to build a simulation mimicking the indentation testing procedures described in Section 5.3.1. The RLI indenter, created using the Solid Edge 3-D modelling package [161] and imported in .SAT format to Abaqus, and the wooden base were assigned typical wooden material properties and meshed with four-noded linear tetrahedral elements and twenty-noded quadratic hexahedral elements respectively. The effect of the pressure mat was simulated using 4-noded quadratic shell elements which modelled membrane forces only, with the properties of rip-stop nylon fabric with polyurethane coating. The parameters for the wood and membrane are shown in Table 8.1 [162, 163]. The cushion foam material was modelled with the Axial + Shear Ogden hyperfoam material model parameters listed in Table 7.1. The cushion was meshed with twenty-noded quadratic hexahedral elements.
The cushion foam material was modelled with the *Axial + Shear* Ogden hyperfoam material model parameters listed in Table 7.1. The cushion was meshed with twenty-noded quadratic hexahedral elements.

Surface to surface contact conditions were imposed at all material interfaces. A coefficient of friction of 0.75 was assumed [67, 164]. The non-linear penalty contact method was applied to model the interaction between the foam material and the indenter. This contact formulation provided good contact solution accuracy and efficient solution times. The model, meshed in Figure 8.1, simulated a displacement of 40.53mm over a time period of 1 minute, followed by a 15 minute dwell period. This simulated the behaviour of the indenter during physical testing. Test results were compared with nodal contact pressures predicted by FEA after the 15 minute dwell period.

![Figure 8.1: Meshed RLI test set-up.](image)

<table>
<thead>
<tr>
<th></th>
<th>Membrane</th>
<th>Wood</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Density (kg/m³)</strong></td>
<td>7.90E+02</td>
<td>6.00E+02</td>
</tr>
<tr>
<td><strong>Young's modulus (GPa)</strong></td>
<td>1.19E+01</td>
<td>9.45E+01</td>
</tr>
<tr>
<td><strong>Poisson's ratio</strong></td>
<td>3.00E-01</td>
<td>4.00E-01</td>
</tr>
</tbody>
</table>

Table 8.1: Material constants for membrane and wood.
8.1.2 Results and validation

Total reaction force was measured from the pressure mat placed beneath the foam and it equalled that of the model and the overall applied load of 1042.5N, this provided basic verification of model constraints, loading and equilibrium. The pressure mat readings from the top and bottom pressure mats are presented in Figure 8.2(a). Smoothing and interpolation algorithms were applied to results from the top pressure mat using the Matlab commercial software package [165] and the results are re-presented in Figure 8.2(b). An FE simulation of the indenter-cushion setup was constructed and solved. Contact pressures from the cushion-indenter interface of this simulation are extracted, smoothed, interpolated and are presented in Figure 8.3. Good overall agreement between the two surface plots was obtained (Figures 8.2(b) and 8.3). Factors such as membrane material creep, indenter imperfections, sensor error and loading differences caused a variance between different loadings due to commonly-experienced issues with pressure mat reliability [166, 167].
Figure 8.2(a): Pressure mat results for loaded indenter, cushion indenter interface on left and cushion-base interface on right (readings in mmHg).

Figure 8.2(b): Top pressure mat reading, results converted to Pa.

Figure 8.3: Contact pressure reading extracted from FE simulation.
Areas of high interfacial pressure in Figures 8.2(b) and 8.3, similarly measured during tests and predicted from FEA, corresponded to the areas on the underside of the leg and the Ischial Tuberosities. Similar contact pressure was also recorded along the sides on the indenter legs and behind. Between the legs, during physical testing, there was no actual contact between the physical indenter and the cushion. However, pressures are caused in this area by the mat being stretched between the underside leg regions. This effect, common when using interface pressure mapping in seating and referred to as ‘hammocking’, is when a pressure mat ‘hammocks’ across the legs, causing a decrease in overall envelopment [166]. The modified pressure mat in the FE simulation allowed for the inclusion of this hammocking effect. For the purpose of clarity, line plots from the two central planes of the model are plotted in a 3-D plot in Figure 8.4, alongside corresponding pressure mat results.

![Pressure Mat reading (Pa)](image)

**Figure 8.4: Pressure extracted from the model and physical testing.**

The model provided accurate results across the range of the top pressure mat. Irregularities on the surface of the wooden indenter meant that some erroneous
pressure readings may have been recorded, especially along the leg region. This was a primary reason for disparity between physical testing pressures and model contact pressures. Overall the model compared well with the results from the indentation procedure, indicating that the model was capable of reasonably accurate complex indentation simulation.

8.2 Viscoelastic material model

8.2.1 FE simulation

The indentation procedure described in Section 5.3.2 was simulated using the commercial FE package, Abaqus [2]. The indenter and the base were modelled as mild steel with a Young’s Modulus of $200 \times 10^9$ N/m$^2$ and a Poisson’s Ratio of 0.3. The cushion foam material was modelled using the Axial + Shear hyperfoam material parameters from Table 7.1 and the Prony Series viscoelastic parameters from Table 7.4. The entire model was meshed with eight-noded hexahedral elements. A quarter-symmetry model was solved, see Figure 8.5.

![Figure 8.5: Quarter symmetry meshed IFD test set-up.](image)

A coefficient of friction of 0.75 was assumed [67, 164]. The penalty contact method was applied between all touching surfaces to model the interactions
between the foam and the indenter. The model simulated indentations at strain rates of $71.4 \times 10^{-5}/s$ and $71.4 \times 10^{-4}/s$. Each indentation was followed by an 8 hour dwell period. This mimicked the behaviour of the forced indenter.

8.2.2 Results and validation

Element deformation patterns from the front edge of the simulated foam, (Figure 8.7), were similar to that of the grid deformation patterns from the deformed foam sample in Figure 8.6. This provided an initial validation of the material model.

In Figures 8.8 and 8.9, force-time data from IFD tests at strain rates of $71.4 \times 10^{-5}/s$ and $71.4 \times 10^{-4}/s$ respectively, were compared with indenter reaction forces predicted by the visco-hyperelastic model throughout an 8 hour simulation period. Results from the hyperfoam model without viscoelasticity (no time dependent relaxation) are provided for comparison with the viscoelastic model. The higher
strain rate model predicted higher peak force, as expected. The highest force values represent the end of the load stage and the beginning of the relaxation period.

The Ogden Hyperfoam model represents material behaviour throughout the load stage and is capable of predicting peak forces accurately. The Prony series parameters, presented in Table 7.4, are responsible for the relaxation behaviour of the material model. The overall shape of the relaxation curve provided by the model is appropriate; however some discrepancies are noticeable throughout the relaxation period. Figure 8.8 shows that after the load period, the 71.4x10^{-5}/s strain rate viscoelastic model begins to diverge slightly from test results. The divergence remains constant in the 4-6% range throughout the relaxation period. The maximum divergence for the 71.4x10^{-4}/s strain rate viscoelastic model (Figure 8.9) occurs in the initial stages, with the model predicting reaction forces that are 10.6% lower than the test results after 60 seconds, reducing to 4% after 600 seconds.
Figure 8.8: Stress relaxation of a sample indented at a strain rate of $71.4 \times 10^{-5}/s$ to a strain of 0.7 by an IFD indenter compared with viscoelastic model predictions.

Figure 8.9: Stress relaxation of a sample indented at a strain rate of $71.4 \times 10^{-4}/s$ to a strain of 0.7 by an IFD indenter compared with viscoelastic model predictions.
Simulating an axial load-relaxation procedure, such as the procedure described in Section 5.2.1, results in good correlation (maximum error < 5%) between model and test results. It must be noted that sample inhomogeneity and small differences in sample size meant the physical IFD test procedures were only repeatable to within ± 5%. Discrepancies may also occur when attempting to simulate the complex IFD test strain patterns using the material model, especially for the material in tension adjacent to the outside edge of the indenter.

Overall, both models compared well with their respective indentation procedures in Section 8.1 and Section 8.2, indicating that the visco-hyperelastic model was capable of complex indentation simulation over extended time periods.

8.3 TTS-WLF

8.3.1 Long-term predictions using TTS-WLF

The predictions generated using the Time Temperature Superposition method in conjunction with the Williams, Landel and Ferry principle (TTS-WLF), presented in Section 7.3, required validation. Accordingly, the creep strain from a physical test conducted at 30°C was compared with the 30°C creep strain TTS-WLF prediction and the 40°C original master-curve (see Figure 8.10). The 30°C test results and TTS-WLF predictions corresponded well throughout the test period. The prediction generated here using the TTS master-curve and WLF method provides a good estimate of long-term creep behaviour (as far as 275 days into the future), which is not normally achievable due to the inordinately long testing periods required. A creep compression test was conducted for validation purposes over a longer time period of 35 days at 30°C. The predictions generated here are proven to be quite accurate up to a 35 day time period (see Figure 8.10).
Figure 8.10: Reference master-curve (45°C), predicted WLF master-curve (30°C) and actual creep test data (30°C).

8.3.2 Short-term prediction at high temperature

The TTS-WLF prediction at the highest temperature of 115°C is presented in Figures 8.11(a) alongside a creep compression validation test, which was also conducted at 115°C and the original master-curve. It was noted that creep predictions took approximately 5% of total predicted test time to become accurate, meaning that predictions at lower time periods (< 5% of total predicted test time) should be disregarded. Higher temperature predictions match physical test results well and the prediction accuracy improves as the test progresses. The higher temperature prediction confirmed using the TTS master-curve and WLF method is extremely useful where high temperature test equipment is not available.
8.3.3 Arrhenius predictions

The Arrhenius model was applied alongside the TTS-WLF theory to the same viscoelastic material to generate an alternative set of creep predictions, based on chemical degradation. Creep threshold values of 1.1, 1.15 and 1.2 normalised creep strain were chosen. Figure 8.12 displays the predictions from the Arrhenius model compared with the physical test data results for the three chosen creep strains. At the higher temperatures, the Arrhenius model predicts the material creep times very well. These accurate higher temperature predictions were expected as the Arrhenius model is a chemically based model and at higher temperatures chemical based changes are expected to dominate.

At lower temperatures, such as 30°C, the predicted time to reach the creep threshold of 1.2 normalised creep strain was 13.3 days. To reach the 1.15
normalised creep strain threshold, at 30°C test temperature, the Arrhenius prediction was 9.9 days. At the same temperature, the Arrhenius model predicted a time of 3.6 days to reach the lowest threshold value of 1.1 normalised creep strain. Actual times to reach threshold values of 1.1 and 1.15 (value extrapolated from the 30°C validation curve) were 10.6 and 63.7 days. These figures indicate that the Arrhenius model significantly under-predicts time to reach creep threshold values at lower temperatures, with inaccuracy increasing as threshold creep strains increase. Ronan *et al.* previously found that temperatures of this magnitude have a similar effect when applying the Arrhenius model to rubber relaxation data [87, 168]. Ronan postulated that the longer times predicted by the Arrhenius model in his research, at temperatures below 70°C, resulted from an underestimation of the physical relaxation processes. This theory held for the test and model results presented here. The shorter times predicted by Arrhenius’ models were assumed to result from an underestimation of the physical creep processes. The results started to diverge below 70°C as physically based viscoelastic creep began to dominate in this temperature region. The results from the Arrhenius model validate previous high temperature predictions. The model also demonstrates that that higher temperature viscoelastic processes are predominantly caused by chemical breakdown and lower temperature processes are caused mainly by physical processes.
8.3.4 Discussion of TTS-WLF and Arrhenius results

TTS-WLF and Arrhenius models were applied separately to the Sunmate blue viscoelastic polyurethane foam material. Extreme value predictions generated using TTS-WLF included short-term high temperature predictions and long-term low temperature predictions.

Predictions generated using TTS-WLF for short-term high temperature creep behaviour were validated and proven to be accurate, especially after 5% of total predicted time.

TTS-WLF lower temperature, longer time predictions have been validated for 35 days. Earlier predictions (< 7% of total predicted time), at 30°C, were less accurate, with accuracy improving as the test progressed. Predictions over-
estimated creep strain times for most of the tests, before becoming more accurate in the latter stages of the prediction. Time estimations to reach a value of 1.1 and normalised creep strain were out by a factor of 1.8 when compared with creep test data. Time estimations to reach 1.13 creep strain were very close to physical test results.

The Arrhenius extrapolation model based on chemical changes was also used as an additional prediction method. The Arrhenius model predicted creep behaviour accurately at higher temperatures, where chemical changes dominated. However, at lower temperatures the model began to underestimate the time required to reach the three chosen threshold creep strain values. The model, at 30°C, underestimated by a factor of 2.9 for the 1.1 threshold and 6.4 for the 1.15 threshold value. It was expected that the Arrhenius prediction would underestimate the time to reach the 1.2 threshold by a factor of 10 or more.

Arrhenius predictions of time required to reach the specific threshold values of 1.1, 1.15 and 1.2 normalised creep strain were larger than the times predicted by the TTS-WLF model by factors of 5.3, 4.6 and 6.9. TTS-WLF predictions were closer than the Arrhenius predictions to physical test time values. The Arrhenius model estimates were shorter than physical test estimates and it is postulated that the model is erroneous due to the absence of chemically based degradation in physical tests at lower test temperatures. Extrapolations were generated from test data at higher temperatures and it is probable that material creep behaviour is based on physical rather than chemical processes at temperatures below 70°C. This corresponds with similar studies on rubber materials [87, 168]. The TTS-WLF predictions generated were more accurate as this model is based on 45°C,
60°C, 70°C and 83°C creep test data, meaning that there was less creep predicted due to high temperature chemical changes.

### 8.4 Thermal conductivity values

To validate thermal conductivity values, physical samples of Sunmate blue and Kayfoam were heated and internal temperatures were measured over time. Samples of 100mm ± 1mm in length, 100 ± 1mm in width and 50mm ± 1mm in height were heated in a Memmert temperature chamber (see Figure 8.13). Temperature was measured from two locations in the foam, 25mm from both edges and 25mm from the upper surface using a NI-9211 temperature measurement module and T-type thermocouples. The results were compared to results from a FE simulation, see Figure 8.14 and 8.15. The thermal conductivity constants used in the FE simulation are displayed in Tables 7.7 for Kayfoam and in Appendix 3 for Sunmate blue. It was noted that the less dense Kayfoam material heated faster than the Sunmate blue material. The model is reasonably consistent with physical testing data for both materials, which verified the accuracy of the conductive constants generated using Equation 3.42.
Figure 8.13: Temperature chamber with NI DAQ temperature measurement equipment in-shot.

Figure 8.14: Comparison of experimental temperature measurements with theoretical model predictions for Sunmate blue material.
Figure 8.15: Comparison of experimental temperature measurements with theoretical model predictions for Kayfoam material.
Chapter 9  Seating simulation results

A range of material model parameters derived and validated in Chapters 7 and 8 are used to simulate polyurethane foam behaviour in the FEA results presented in this chapter. Temperature dependent Ogden Hyperfoam parameters are used in conjunction with appropriate Prony series viscoelastic parameters to simulate the structural behaviour of the material. Theoretical parameters are also used to simulate thermal conductivity. FE simulations are conducted on a range of theoretical cushion designs. Specific designs included cushions of varying contoured shapes constructed from two different materials, separately or in combination. The relative pressure relieving performance of each cushion design is comparatively discussed. Conclusions are suggested about the relative performance of the different materials and cushion designs.

9.1 Model description

Separate simulations were conducted on eight different cushion designs, two plain material cushions and six layered cushions; of which, two were un-contoured and four were pre-contoured. Cushion designs are explained below and a short-handed name, for use during discussion of results, is provided in brackets. Each cushion design had an initial length of 400mm, width of 100mm and height of 70mm. The two standard cushions were respectively fabricated from Sunmate blue material (Sunmate) and Kayfoam material (Kayfoam). Two un-contoured layered cushions were simulated, both cushions consisted of a 25mm topping of one material layered over a 50mm base of the other material, i.e. 25mm Kayfoam on top of 50mm Sunmate (Kayfoam topping) and 25mm Sunmate on top of 50mm Kayfoam (Sunmate topping). Four pre-contoured versions of the layered cushions
were also modelled. Kayfoam topping and Sunmate topping cushions were indented a distance of 10mm, the global displacements after these indentations were recorded. These models of the contoured cushions (10mm pre-contoured Kayfoam topping and 10mm pre-contoured Sunmate topping) were then saved as unstressed cushions, see Figure 9.1.

![Figure 9.1: 10mm pre-contoured cushion, prior to simulation.](image1)

The same procedure and an initial indentation of 25mm, was used to create the final two cushion designs (25mm pre-contoured Kayfoam topping) and (25mm pre-contoured Sunmate topping), refer to Figure 9.2.

![Figure 9.2: 25mm pre-contoured cushion, prior to simulation.](image2)

The basic simulation assembly consisted of a flat cushion resting on a wooden base being loaded by a wooden indenter (Figure 9.3). The indenter, which was simplified but similar to the RLI buttock shaped indenter used in validation methods described in section 8.1, was generated in the Solid Edge 3-D modelling package [161] and exported in .SAT drawing format to the Abaqus FE package
The symmetrical nature of the set-up allowed the use of a half model exploiting the axis of symmetry. Identical boundary conditions and loading steps were applied to each separate cushion simulation. A load, equivalent to the 50th percentile male weight [170], was applied by a 37°C indenter. The suitability of this temperature was verified by previous studies on human-cushion interface temperatures [94, 171]. This load was then held constant over an eight hour period of time which represented an estimate of the average wheelchair user’s daily occupancy. A more detailed review of the boundary conditions, loads, interactions and mesh applied is presented in Appendix 4.

All results were extracted from the top layer of the foam material; results from this area were the most appropriate for an analysis of pressure relieving performance. Research has shown that different combinations of direct pressure and shear (for example high shear and medium pressure or high pressure and medium shear) when applied to the outside of the skin still have the same effect beneath the skin. In this way it was demonstrated that direct pressure and shear stress were the two most important components in ulcer formation [6, 120, 144]. Therefore, it was decided that contact pressure and the magnitude of contact shear force from a 50th percentile, 37°C loading were the most relevant results to be
determined; these results were presented for each cushion design. Contact pressure (results in Pa) is defined as the normal contact force divided by the cross-sectional area at the contact node, the cross-sectional area associated with every node is known through the definition of element size. Contact shear force (results in µN) can occur when two forces are in opposite directions such that there is a deformation, or attempted deformation, of the tissue in parallel planes. Contact shear force can also occur when two forces act in the same direction but with different magnitudes, known as pinch shear stress. Both normal contact force and friction are required to create contact shear force. To provide a greater understanding of in-service material behaviour, a full cushion temperature profile and nodal displacement plot were also presented for the Sunmate cushion only (temperature and displacements profiles were similar for all cushion designs).

### 9.3 Simulation results

#### 9.3.1 Cushion temperature

A nodal temperature plot of the indented Sunmate cushion is displayed in Figure 9.4. The temperature at the interface reached indenter temperature rapidly for all simulations and the internal node, marked in Figure 9.4, took longer to reach a steady temperature state (see Figure 9.5).

![Figure 9.4: Sunmate cushion nodal temperature, internal node marked in white.](image)
Figure 9.5: Time dependent nodal temperature for the Sunmate cushion, taken from three locations at the indenter-cushion interface and one internal location.

9.3.2 Cushion displacement

Interface displacement was also recorded for the Sunmate cushion and is displayed in Figure 9.6. Maximum displacement was 35.24mm; this remained largely unchanged throughout the eight hour simulation, as it did for all cushion designs.

Figure 9.6: Sunmate cushion displacement immediately before the final step.
9.3.3 Contact pressure

Contact pressure results are presented here for each proposed cushion design. Snapshots which represent indenter-cushion interface contact pressure immediately after the loading step \((t = 0)\) and immediately after the final step \((t = 8 \text{ hours})\) are displayed for each cushion from Figure 9.7 to Figure 9.10. For ease of comparison, the legend from the first simulation (Figure 9.7) is utilised for all snapshots. Data was extracted from the IT region (marked white in Figure 9.7) of all cushion designs for subsequent comparisons of time dependent contact pressure. Time dependent nodal data from all three nodal locations marked in Figure 9.7 is presented for each design in Appendix 5.

![Figure 9.7: Sunmate (left) and Kayfoam (right) interface contact pressure (Pa) at \(t = 0\) and \(t = 8h\). Dots on the left hand side plot represent nodes from which time dependent data is extracted for future figures.](image-url)
Figure 9.8: Interface contact pressure (Pa) at $t = 0$ and $t = 8h$ for Kayfoam topping cushion (left) and Sunmate topping cushion (right).

Figure 9.9: Interface contact pressure (Pa) at $t = 0$ and $t = 8h$ for 10mm pre-contoured Kayfoam topping cushion (left) and 10mm pre-contoured Sunmate topping cushion (right).
Figure 9.10: Interface contact pressure (Pa) at $t = 0$ and $t = 8h$ for 25mm pre-contoured Kayfoam topping cushion (left) and the 25mm pre-contoured Sunmate topping cushion (right).

9.3.4 Contact shear force

Contact shear force surface plots are presented here for each proposed cushion design. Snapshots which represent indenter-cushion contact shear force before the final time step ($t = 0$) are displayed for each separate cushion design in Figure 9.11 and Figure 9.12. Contact shear force is the resultant force of contact shear force components in the x, y and z directions, explained in more detail for the Sunmate cushion in Appendix 6.
Figure 9.11: Magnitude (resultant) of contact shear force at $t = 0$ for (top to bottom)

Sunmate cushion, Kayfoam cushion, Kayfoam topping and Sunmate topping.
Figure 9.12: Magnitude (resultant) of contact shear force at $t = 0$ for (top to bottom) 10mm pre-contoured Kayfoam topping cushion, 10mm pre-contoured Sunmate topping cushion, 25mm pre-contoured Kayfoam topping cushion and 25mm pre-contoured Sunmate topping cushion.
9.4 Analysis of results

The results presented in section 9.3 are analysed in this section. Contact pressure and contact shear force, which are two prominent extrinsic factors of pressure ulcer occurrence, are discussed separately and then compared for eight different cushion designs.

9.4.1 Contact pressure

The boundary conditions and loading patterns applied to each simulated cushion design were identical. As previously discussed in Chapter 4 of the literature review of this thesis, the IT region is recognised as an area where pressure can cause discomfort and potential skin damage to wheelchair users. Therefore, the maximum contact pressure in the IT region for each cushion design is plotted and tabulated over a time period similar to that of a regular daily occupancy (see Figure 9.13 and Table 9.1).

The plain Kayfoam cushion displayed lower initial contact pressures than the plain Sunmate cushion, which displayed the highest initial contact pressure of all simulated designs. However, the viscoelastic Sunmate material, as expected, tended to relax over time which meant that both cushions displayed similar contact pressures at the end of the 8 hour period. The Sunmate topping layered cushion displayed the second highest contact pressure initially, although this was quickly followed by a sharp drop in pressure (11.2% reduction over the final step). The Kayfoam topping cushion displayed slightly lower initial contact pressure, however the 25mm layer of conventional material on top did not allow for the same amount of pressure drop (4.72% reduction over the final step). Therefore, the final contact pressure was lower for the layered cushion with viscoelastic Sunmate topping. It was also noted that the Kayfoam topping cushion
demonstrated the highest contact pressure of all cushions after the eight hour loading period; this was due to its relatively high initial contact pressure coupled with its low rate of stress relaxation. The Sunmate cushion displayed 0.7% higher final contact pressure than the Kayfoam cushion and the Sunmate topping displayed 12.4% lower final contact pressure than its Kayfoam topping counterpart.

Figure 9.13: IT region contact pressure over a simulated daily seating period using eight different cushion designs.
<table>
<thead>
<tr>
<th>Cushion</th>
<th>Contact pressure(_{\text{initial}}) (Pa)</th>
<th>Contact pressure(_{\text{final}}) (Pa)</th>
<th>Pressure Change (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Sunmate cushion</td>
<td>11405</td>
<td>9864</td>
<td>-13.51</td>
</tr>
<tr>
<td>Kayfoam cushion</td>
<td>9907</td>
<td>9800</td>
<td>-1.08</td>
</tr>
<tr>
<td>Sunmate topping</td>
<td>10659</td>
<td>9465</td>
<td>-11.20</td>
</tr>
<tr>
<td>Kayfoam topping</td>
<td>10517</td>
<td>9998</td>
<td>-4.93</td>
</tr>
<tr>
<td>10mm Cont. (Kayfoam top)</td>
<td>8702</td>
<td>8291</td>
<td>-4.72</td>
</tr>
<tr>
<td>10mm Cont. (Sunmate top)</td>
<td>8255</td>
<td>7377</td>
<td>-11.90</td>
</tr>
<tr>
<td>25mm Cont. (Kayfoam top)</td>
<td>7619</td>
<td>7563</td>
<td>-0.74</td>
</tr>
<tr>
<td>25mm Cont. (Sunmate top)</td>
<td>7218</td>
<td>7217</td>
<td>-0.01</td>
</tr>
</tbody>
</table>

Table 9.1: Tabulated values of contact pressure immediately before and immediately after the daily sitting period.

The same layered cushions as before were utilised in the contoured cushion simulations. The 10mm pre-contoured Kayfoam topping and 10mm pre-contoured Sunmate topping cushions exhibited the 5\(^{th}\) and 6\(^{th}\) highest initial contact pressures respectively. The 10mm pre-contoured Kayfoam topping cushion displayed higher initial contact pressure and lower levels of stress relaxation than the 10mm pre-contoured Sunmate topping cushion. The percentage drop in pressure over the period of the simulation for the 10mm pre-contoured Kayfoam and Sunmate topping cushion was 4.72% and 11.9% respectively, which correlated with the un-contoured layered cushions time dependent behaviour. The second lowest and lowest initial contact pressures in the IT area were recorded from the 25mm pre-contoured Kayfoam topping and Sunmate topping cushions. It was also noted that the 10mm pre-contoured Sunmate topping cushion final contact pressure was less than the final contact pressure of the 25mm pre-contoured Kayfoam topping cushion. The time-dependency recorded for the two 25mm pre-contoured cushion designs was extremely low (< 1% for both). This was because overall surface contact area increased substantially for this type of pre-contoured cushion. The high level of surface area resulted in increased surface tension, which caused the increased surface friction effects to counteract most of the viscoelastic effects of
the top layer of material. This resulted in minimisation of viscoelastic stress relaxation behaviour. The higher contact area of the contoured cushion was the reason for their lower contact pressures.

9.4.2 Contact shear force

Values of maximum elemental contact shear force in the IT region were also extracted throughout each simulation. It was noted from analysing Figure 9.14 and Table 9.2 that the four contoured cushions displayed significantly higher contact shear forces than the rest of the cushion designs.

Figure 9.14: Maximum elemental shear contact force in the IT region over a daily seated period.
The 25mm pre-contoured cushions displayed the highest contact shear force of all cushions in the IT region. The 10mm pre-contoured Kayfoam topping and 10mm pre-contoured Sunmate topping displayed very similar contact shear forces. Near identical shear contact forces were also recorded for the two 25mm pre-contoured Sunmate topping cushions. The un-contoured Kayfoam topping cushion displayed the next highest contact shear force, closely followed by the plain Kayfoam cushion. The plain Sunmate cushion and the Sunmate topping cushion produced the second lowest and lowest initial contact shear force results. The Sunmate cushion and Sunmate topping cushion produced final contact shear forces of 20.3% and 45.1% lower than the Kayfoam and Kayfoam topping cushions.

### 9.5 Summary

In flat cushion simulations Kayfoam material demonstrated lower direct contact pressure values than Sunmate material. It was noted that the lower density Kayfoam material allowed for higher surface contact area and therefore increased envelopment, this was the reason for the lower contact pressures recorded for the
material. For all contoured cushion designs however, it was shown that Sunmate topping cushions material exhibited lower direct contact pressures.

The Sunmate material displays lower contact shear values than the Kayfoam material, with lower contact shear forces recorded for the Sunmate and Sunmate topping cushions when compared with the Kayfoam and Kayfoam topping cushions.

The viscoelastic Sunmate material exhibits its viscoelasticity in the form of stress relaxation in seating, this was evidenced when analysing the simulations of contact pressure values over time for this material. The standard Sunmate cushion displays relatively high contact pressures before relaxing substantially to almost match the direct contact pressure performance of the Kayfoam material cushion at the end of the simulated period. The Sunmate material displayed more viscoelastic effects in all simulations when compared with the Kayfoam conventional material. In clinical practise, when using interface pressure mapping to aid in the prescription of wheelchair cushioning systems, sitting time periods of between 6 and 10 minutes have been advocated as suitable time scales prior to recording pressure readings [137, 172]. The results presented here would suggest that this is not a suitable settling period, especially when using viscoelastic material, as stress relaxation is prominent over the first 40-45 minutes of a test.

The layered cushions produced contrasting results with reductions in pressure and shear force found with the layered Sunmate topping cushion by comparison with a Sunmate cushion. This was in contrast to increases in pressure and shear force exhibited by a layered Kayfoam topping cushion compared with a standard Kayfoam cushion.
Temperature is also known to affect the contact pressure-time relationship. Knowledge about the magnitude of this effect and its relationship with external pressure remained largely un-quantified until Lachenbruch used Kokate’s temperature equivalence results and Reswick and Rogers’ pressure-time results to generate predictions of the relationship between pressure ulceration risk, time, temperature and pressure magnitude [94, 115, 128]. Simulated contact pressure values are compared to Lachenbruch’s time-temperature-pressure predictions in Figure 9.15 to give an insight into how these pressure time simulations may prove beneficial to seat prescribing clinical engineers.

![Figure 9.15: Pressure-time-temperature relation. The curved line signifies average probability of pressure ulcer occurrence [94]. Time dependent contact pressure is also displayed.](image)

Pressure-time-temperature co-ordinates plotted below the curve represent cases where there is a lower than average probability of ulceration and pressure-time-temperature co-ordinates plotted above the curves represent situations where there
is a higher than average probability of ulceration. These results are intended to demonstrate the capacity of the range of material parameters to model FPF behaviour. The results of these simulations when compared to Lachenbruch’s theoretical curves prove that the methodology used in this work is credible.

A body of clinical data exists which links time, temperature and direct pressure with the possible onset of pressure ulcers. Although there is evidence relating shear to superficial (grade 1 and grade 2) pressure ulcers, there is no equivalent definitive body of work for shear force. Therefore, since variations in envelopment and contact area which result in changes to contact pressure can also affect shear, it is currently difficult to make clinical judgements based on these models alone. However, based on clinical engineering considerations, these models are felt to provide adequate comparative performance.

It has been found that, as expected, pre-contoured cushions allowed for increased contact area during seating, this leads to an overall reduction in direct pressure. Larger pre-contours have also been found to result in lower direct pressures. In this study, the four pre-contoured cushions displayed substantially lower contact pressures than all other cushions. A reduction of final contact pressure of 32.2% was recorded between the un-contoured Kayfoam topping cushion and the Kayfoam topping cushion with a 25mm pre-contour. A similar reduction of 31.1% was found for the Sunmate topping cushion, un-contoured and 25mm pre-contoured. However, the shear contact pressure in the IT region was substantially higher for the pre-contoured cushions than all other cushions. Shear forces for the Kayfoam topping cushion with 25mm pre-contour were shown to be 203.1% higher than an identical un-contoured cushion. This increased to 444.5% when comparing the Sunmate topping cushion with 25mm pre-contoured Sunmate
topping material. These findings indicate that as peak pressures are removed from the high risk IT region using pre-contoured cushions, higher shear forces are created due to an increase in surface tension which is caused by the larger surface area. Deeper initial contours therefore create an environment for higher contact areas, resulting in lower direct contact pressures and higher contact shear forces. This study therefore supports Brienza’s clinical findings – that shear is higher when using personalised pressure relieving seating solutions (pre-contoured cushions) compared with flat cushions [123, 138].
Chapter 10 Conclusions and future work

10.1 Conclusions

A methodology to develop a combination of temperature dependent compressible hyperelastic, long-term viscoelastic and thermally conductive parameters for FPF was established. Accurate temperature-dependent Ogden Hyperfoam parameters were developed using curve-fitting procedures with results from static compression and simple shear tests. Maxwell-Wiechert and Kelvin-Voigt multiple models were utilised to model viscoelastic phenomena. Parameters for these spring-dashpot combinations have been obtained from stress relaxation and compressive creep test results. Theoretical thermal conduction parameters, obtained from the literature and from inspection of relevant samples, were capable modelling of foam’s thermal behaviour. These parameters, in conjunction with temperature-dependent Hyperfoam parameters and time-dependent viscoelastic parameters, allow for the simulation of temperature-based material softening.

Validation has been achieved for all hyperelastic and viscoelastic model parameters by comparing physical sample indentation with results from FE simulations of identical procedures. Thermal conductivity parameter sets were validated using an inverse modelling procedure. This particular combination of material parameters provide a unique ability to simulate in-service time and temperature dependent wheelchair foam behaviour and can be utilised in applications with similar thermal and structural boundary conditions.
A method for generating predictions for material behaviour beyond the scope of testing from a range of short-term test procedures was developed for FPF using TTS, WLF and Arrhenius theories. Long-term creep predictions for polyurethane foam, as far as 275 days, were generated from short-term test data conducted in less than 4 days. Predictions have been validated up to 35 days. Fully validated creep predictions at high-temperatures (115°C) for polyurethane foam have been generated from tests conducted up to 83°C. Predictions generated for long-term performance of polymer foams have also been generated using the Arrhenius model. Using this method, it has been found that lower temperature predictions begin to diverge; this finding is in agreement with results on similar materials. The procedure presented here enables accurate prediction of creep compressive behaviour for temperatures or time-scales which are not easily achievable in the laboratory.

The models created in this work have been shown to be capable of producing values for surface pressure, shear and temperature for the materials considered using an approximate simulation of human contact. This simulation was deemed to provide an adequate basis for comparison of variations encountered in real world applications. Results from the FEA model, using previously described model parameters, allows for:

- Analysis of time dependent behaviour of material. The Sunmate material displayed higher viscoelastic stress relaxation in all simulations when compared with the Kayfoam conventional material. This had the effect of decreasing contact pressure values over time at the seating interface. In current clinical practice, when using interface pressure mapping to aid in the prescription of wheelchair cushioning systems, time periods of
between 6 and 10 minutes have been advocated as suitable intervals between initial sitting and the recording of pressure readings. The results presented here suggest that this is not a suitable settling period, especially when using a viscoelastic material, as stress relaxation is prevalent over the first 40-45 minutes of the test.

- Analysis of the effects of cushion shape on pressure and shear. Pre-contoured cushions were found to offer particularly low contact pressures by comparison with all other cushion configurations. This was because of the increased contact area obtained from this cushion design. Larger contour depths were found to result in lower direct pressures. Conversely, the contact shear force in the IT region was substantially higher for the pre-contoured cushions than all other cushions. These findings indicate that as peak pressures are diverted from the high risk IT region using pre-contoured cushions, higher shear forces are created due to an increase in surface shear. Deeper initial contours therefore permit higher contact areas, resulting in lower direct contact pressures and higher contact shear forces. This study therefore supports qualitative findings from clinical trials, which found that shear is a more prominent causative factor of pressure ulcer formation using pre-contoured cushions by comparison with compressive forces in flat cushions [123, 138].

- Correlation of results by comparison with results from clinical trials. Results can be compared to clinical based trial results such as that of Lachenbruch or Laizzo [94, 115, 128] [131].
10.2 Future work

The methodologies used here could be utilised to generate parameters describing the behaviour of any FPF material. There are potentially numerous novel applications in the use of long-term behavioural predictions generated from high temperature test procedures for generating viscoelastic parameters in a range of elastomeric materials (e.g. life prediction of insulation hoses carrying coolant in engines, set and change of modulus in earthquake isolation bearings). The viscoelastic parameters determined in this work may be used in any application where FPF is under constant loading or subjected to a constant strain.

The FE simulations conducted during this thesis could be expanded to encompass a wider range of materials used in the area of wheelchair seating. Additionally, a wider range of cushion designs could be simulated. Further loading cases, different standard cushion dimensions and alternative layering patterns could also be a topic of interest in further research. Conducting a high number of pressure map readings over time and averaging results could help to validate the results presented here. Problems with this method include the reliability and repeatability of pressure map readings, the hammocking effect created by the pressure mat and the thermal effects of having such a mat in place. MRI scanning is another method which could be used to measure strain in a seating situation. This could strengthen the procedures for validating computer simulation models.

The FE models created in Chapter 9 could be used to perform large-scale parametric studies of cushion types to establish design guidelines. Further clinical trials would be useful in verifying the predicted results.
One aim is for the outcomes of this research to inform clinical trial procedures. Clinical trials could thereafter lead to a credible ranking of the range of cushions and materials considered in this programme and thus have a substantial influence on material selection for wheelchair user comfort and wellbeing.

Run times of finite element models were found to be quite long when using complex shaped indenters. It is suspected that the contact algorithms and/or meshing issues are the cause of this. It is recommended that some consideration be given to alternative solution methods (alternative codes, element technologies, contact algorithms or mesh-free methods).

Utilisation of a more realistic human indenter is another interesting avenue of future study. Material properties for human muscle, skin and fat could be utilised with a more realistic human buttock shape. The parameters generated from this work would also prove useful to researchers already conducting studies on tissue-bone dynamics in cushioned seating.

Other areas of seating such as aircraft and automobile, where seating is used with limited scope for mobility, could benefit from utilising material parameters or methodologies for material model development presented here.

Further validation of these models could be carried out by more accurate measurements than those provided by the pressure mat technology in current use. Accurate measurements of strain, temperature and interface forces over long time periods would add to the validity of the models.
A comfort criterion based on temperature, pressure and time (and possibly other parameters available in this model) matched to human perception could be used to inform and develop wheelchair seating prescription practises.
**Glossary of terms**

**Aetiology:** The science that deals with the causes or origin of disease, the factors which produce or predispose toward a certain disease or disorder.

**Capillary:** A minute vessel which contains fluid, mainly blood.

**Coccyx:** The small tail-like bone at the bottom of the spine very near to the anus. The coccyx is made up of 3-5 rudimentary vertebrae. It is the lowest part of the spinal column.

**Deviatoric stress:** Stress component in a system which contains unequal principal stresses. Deviatoric stress controls the degree of distortion.

**Facia:** A sheet or band of fibrous tissue lying deep within the skin.

**Gluteal muscle:** Any one of three large skeletal muscles that form the buttock and move the thigh.

**Hypocycloid:** The plane locus of a point fixed on a circle that rolls on the inside circumference of a fixed circle.

**Ischemic deficit:** The reduction in blood flow to a bodily organ caused by constriction or blockage of blood vessels.

**Ischial Tuberosities:** Rounded protuberances of the lower part of the ischium. Forms a bony area on which the human body rests when in a sitting position.

**Kinematics:** The branch of mechanics that studies the motion of a body or a system of bodies without consideration given to its mass or the forces acting on it.
**Lymphatic system:** The network of vessels through which lymph drains from the tissues into the blood.

**Macrostructure:** Structure visible to the naked eye.

**Maceration:** The softening and breaking down of skin resulting from prolonged exposure to moisture.

**Metabolic:** Relating to metabolism, the whole range of biochemical processes that occur within humans.

**Myogenic:** Giving rise to or forming muscular tissue.

**Necrosis:** Pathologic death of one or more cells, or of a portion of tissue or organ, resulting from irreversible damage. After such changes, the outlines of individual cells are indistinct, and affected cells may merge, sometimes forming a focus of coarsely granular, amorphous, or hyaline material.

**Neurological:** Having to do with the nerves or the nervous system.

**Occlusion:** An obstruction or a closure of a passageway or vessel.

**Paraplegia:** Complete paralysis of the lower half of the body including both legs, usually caused by damage to the spinal cord.

**Pathology:** The scientific study of the nature of disease and its causes, processes, development, and consequences.

**Pelvis:** The lower (caudal) portion of the trunk, bounded anteriorly and laterally by the two hip bones and posteriorly by the sacrum and coccyx.
Perfusion: Injection of fluid into a blood vessel, usually to supply nutrients and oxygen.

Posterior thigh muscle: The posterior thigh muscles are part of a grouping of muscles which flex the knee and stretch the hip.

Prognostic: Predicting the likely outcome of a disease; of or relating to a prognosis.

Tetrakaidecahedra: A solid shape with fourteen sides.

Tissue oxygenation: The process whereby oxygen molecules (O2) enter the tissues of the body.

Von Mises stress: Geometrical combination of all the stresses (normal stress in the three directions, and all three shear stresses) acting at a particular location.
Bibliography


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[141] Lobo H and Bethard T. Practical issues in the development and implementation of hyperelastic models In Simulia Customer Conference, London.


Appendix 1

### List of Material Test Procedures

<table>
<thead>
<tr>
<th>Parameter Identification</th>
<th>Kayfoam</th>
<th>Sunmate B.</th>
<th>Sunmate G.</th>
</tr>
</thead>
<tbody>
<tr>
<td>Axial Compression (20°C)</td>
<td>Kayfoam</td>
<td>Sunmate B.</td>
<td>Sunmate G.</td>
</tr>
<tr>
<td>Axial Compression (30°C)</td>
<td>Kayfoam</td>
<td>Sunmate B.</td>
<td>Sunmate G.</td>
</tr>
<tr>
<td>Axial Compression (37°C)</td>
<td>Kayfoam</td>
<td>Sunmate B.</td>
<td>Sunmate G.</td>
</tr>
<tr>
<td>Simple Shear</td>
<td>Kayfoam</td>
<td>Sunmate B.</td>
<td>Sunmate G.</td>
</tr>
<tr>
<td>Creep Compression (45°C)</td>
<td>Kayfoam</td>
<td>Sunmate B.</td>
<td>Sunmate G.</td>
</tr>
<tr>
<td>Creep Compression (60°C)</td>
<td>Kayfoam</td>
<td>Sunmate B.</td>
<td>Sunmate G.</td>
</tr>
<tr>
<td>Creep Compression (70°C)</td>
<td>Kayfoam</td>
<td>Sunmate B.</td>
<td>Sunmate G.</td>
</tr>
<tr>
<td>Creep Compression (85°C)</td>
<td>Kayfoam</td>
<td>Sunmate B.</td>
<td>Sunmate G.</td>
</tr>
<tr>
<td>Stress Relaxation</td>
<td>Kayfoam</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Parameter Validation</th>
<th>Kayfoam</th>
<th>Sunmate B.</th>
<th>Sunmate G.</th>
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</thead>
<tbody>
<tr>
<td>ISO 16840-2</td>
<td>Kayfoam</td>
<td>Sunmate B.</td>
<td>Sunmate G.</td>
</tr>
<tr>
<td>ISO 2439</td>
<td>Kayfoam</td>
<td>Sunmate B.</td>
<td>Sunmate G.</td>
</tr>
<tr>
<td>High-Temp. Creep Comp. (115°C)</td>
<td>Kayfoam</td>
<td>Sunmate B.</td>
<td>Sunmate G.</td>
</tr>
<tr>
<td>Long-term Creep Comp. (35 days)</td>
<td>Kayfoam</td>
<td>Sunmate B.</td>
<td>Sunmate G.</td>
</tr>
<tr>
<td>Sample Heating Procedure (37°C)</td>
<td>Kayfoam</td>
<td>Sunmate B.</td>
<td>Sunmate G.</td>
</tr>
</tbody>
</table>

Table A.1.1: List of all parameter identification and validation tests and which materials they were conducted on.

<table>
<thead>
<tr>
<th>Strain rate (1/s)</th>
<th>Corresponding crosshead speed (mm/min)</th>
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<tbody>
<tr>
<td>143x10^-6</td>
<td>1</td>
</tr>
<tr>
<td>714x10^-6</td>
<td>5</td>
</tr>
<tr>
<td>714x10^-5</td>
<td>50</td>
</tr>
<tr>
<td>14.28x10^-3</td>
<td>100</td>
</tr>
<tr>
<td>35.7x10^-3</td>
<td>250</td>
</tr>
<tr>
<td>71.4x10^-3</td>
<td>500</td>
</tr>
</tbody>
</table>

Table A.1.2: List of relevant strains and corresponding crosshead speeds.
Appendix 2

Shown below are the test temperatures for the temperature dependent quasi-static compressive tests conducted on the Sunmate blue, Sunmate grey and Kayfoam materials.

Figure A.2.1: Testing temperatures for the duration of all tests conducted in Figures 6.1, 6.2 and 6.3.
Appendix 3

Thermal conductivity values were calculated for the Sunmate blue material using the same method presented in section 7.4 in the generation of values for Kayfoam. The values are presented in Figure A.3.1. The table of values used to generate the thermal conductivity values is presented in Table A.3.1.

Figure A.3.1: Sunmate blue thermal conductance values calculated at three temperatures and nine strain values.
<table>
<thead>
<tr>
<th>Symbol (Unit)</th>
<th>Variable</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>$K_w (m^{-1})$</td>
<td>Extinction coefficient of cell wall material</td>
<td>6.00E+04</td>
</tr>
<tr>
<td>$d$ (m)</td>
<td>Cell diameter</td>
<td>1.56E-04</td>
</tr>
<tr>
<td>$f_s$</td>
<td>Fraction of solid in strut</td>
<td>9.50E-01</td>
</tr>
<tr>
<td>O.C.</td>
<td>Open cell fraction</td>
<td>8.50E-01</td>
</tr>
<tr>
<td>$\lambda (W.m^{-1}.K^{-1})$</td>
<td>Thermal conductivity of polymer</td>
<td>2.60E-01</td>
</tr>
<tr>
<td>$\rho_f (Kg.m^{-3})$</td>
<td>Density of PU foam</td>
<td>8.00E+01</td>
</tr>
<tr>
<td>$\rho_s (Kg.m^{-3})$</td>
<td>Density of PU polymer</td>
<td>1.20E+03</td>
</tr>
<tr>
<td>$\sigma (W.m^{-2}.K^{-4})$</td>
<td>Boltzmann constant</td>
<td>5.70E-08</td>
</tr>
<tr>
<td>$k_{air 20^\circ C} (W.m^{-1}.K^{-1})$</td>
<td>Thermal conductivity of air</td>
<td>2.69E-02</td>
</tr>
<tr>
<td>$K (m^{-1})$</td>
<td>Extinction coefficient</td>
<td>1.58E+04</td>
</tr>
<tr>
<td>$\delta$</td>
<td>Volume fraction of voids or cell interiors</td>
<td>6.67E-01</td>
</tr>
</tbody>
</table>

Table A.3.1: Values used in the generation of Sunmate blue thermal conductance values.
Appendix 4

A.4.1 Standard boundary conditions and load steps

A pre-defined temperature of 20°C was applied to mimic room temperature. Symmetrical conditions were defined through the central plane of the simulation. The lower side of the wooden base was constrained in the x, y and z directions and a non-zero displacement condition was applied to one end of the indenter to ensure it displaced uniformly and in parallel with the wooden base.

The first step of the simulation consisted of a displacement of 1.5mm, which was applied vertically downwards onto the top of the indenter. The initial displacement allowed the cushion and indenter to come into contact, this avoided convergence problems which arose when initially applying a load instead of a displacement. The next step was the loading step, in which the initial displacement of the indenter was made inactive and a pressure load representing the 50$^{th}$ percentile male (78kg) was applied. In the final step, this pressure load was held constant over an 8 hour period of time. The temperature of the indenter was held at 37°C using a boundary condition which was applied instantaneously in the final step of the simulation.

A.4.2 Material interactions

Normal, tangential and thermal interaction properties were defined to represent the interaction between the indenter and the cushion. A non-linear penalty stiffness algorithm was applied, with a friction coefficient of 0.75 [67, 164]. The table of pressure-thermal conductivity values, displayed in Table 7.7, is used here to model the thermal interaction at various strain levels for Kayfoam material.
Table A.3.1, presented in Appendix 3, represents the thermal conductivity behaviour of Sunmate material over the same strain range.

A.4.3 Simulation mesh

The RLI indenter used for validation purposes in section 8.1 was a complex shape which could only be meshed using tetrahedral elements, see Figure 8.1. These elements used in contact problems with hexahedral elements are generally less accurate because of a faceting effect which can cause incorrect stress peaks. Second order (quadratic) hexahedral elements could potentially have been used to improve simulation accuracy. However, in Abaqus these elements are often problematic when used in a non-linear problem including hyperelasticity and contact. Discrete rigid first order (linear) hexahedral shell elements could provide accurate stress values, however these elements are not temperature enabled. A slightly geometrically simplified version of the indenter used in section 8.1 was therefore generated using Solid Edge, see Figure A.4.1.

![Simplified indenter (Half size).](image)

The simplified version had a smoother surface and which made it easier to mesh using the partitioning tool in Abaqus. Three-dimensional first order coupled
temperature-displacement hexahedral elements (Abaqus C3D8T elements) were therefore used for each part throughout the simulation. The global use of hexahedral elements helped eradicate many of the contact issues prevalent when using the original indenter.

Some mesh convergence analysis was conducted on the model (Figure A.4.2), with maximum nodal displacement defining model accuracy. A global element size (general size of each element’s side, not including areas with specific element sizing) of 12 mm was found to provide suitably accurate results. It was felt that improvements in model accuracy generated by increasing mesh density to a global element size of 9 mm did not warrant the increase in computational time.

![Figure A.4.2: Results from mesh sensitivity analyses, curve converging towards the left of the graph.](image)

The finalised model had a total of 12400 nodes, with a solve time which varied from 1.5 hours up to 8.5 hours (depending on boundary conditions) using 2GB of
RAM on an Intel® Core™ 2 processor. Much improved solve times were obtained by using the facilities at the Irish Centre for High End Computing (ICHEC). Processor sensitivity analysis was performed to ensure efficient use of the significant computational resources available here. The ICHEC facility greatly enhanced the author’s productivity.
Appendix 5

Time dependent nodal contact pressures for each cushion design are presented separately here for the three nodal locations referred to in Figure 9.8.

Figure A.5.1: Time dependent contact pressure, taken at three nodal locations on the indenter-Sunmate cushion interface.

Figure A.5.2: Time dependent contact pressure, taken at three nodal locations on the indenter-Kayfoam cushion interface.
Figure A.5.3: Time dependent contact pressure, taken at three nodal locations on the indenter-Kayfoam topping cushion interface.

Figure A.5.4: Time dependent contact pressure, taken from three nodal locations on the indenter-Sunmate topping cushion interface.
Figure A.5.5: Time dependent contact pressure, taken at three nodal locations on the indenter-10mm pre-contoured Kayfoam topping cushion interface.

Figure A.5.6: Time dependent contact pressure, taken at three nodal locations on the indenter-25mm pre-contoured Kayfoam topping cushion interface.
Figure A.5.7: Time dependent contact pressure taken at three nodal locations on the indenter-10mm pre-contoured Sunmate topping cushion interface.

Figure A.5.8: Time dependent contact pressure taken at three nodal locations on the indenter-25mm pre-contoured Sunmate topping cushion interface.
Appendix 6

The contact shear force for the 37°C simulation was analysed from Figure A.6.1 to Figure A.6.4. Figure A.6.1 signifies the maximum contact shear force in the x-direction. The highest blue values in Figure A.6.2 represent maximum contact shear force in the y-direction. Figure A.6.3 represents the contact shear force caused by pinch shear stress, with high forces recorded in both the x-direction and the y-direction. The magnitude of contact shear force, Figure A.6.4, is the resultant of the three components.
Figure A.6.3: Contact shear force in the z-direction after the final step.

Figure A.6.4: Magnitude (resultant) contact shear force after the final step.
List of Publications

Journal papers


Conference papers


Conference posters


